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Uncertainties in the estimation of fluid loading on offshore structures with special emphasis on wind forces

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SYNOPSIS

Some of the major uncertainties in the estimation of mean drag forces on offshore structures, especially those generated by wind, are presented and discussed. Standard techniques, as suggested by published criteria and guidelines, have been used to illustrate some of these uncertainties for three cases. These are loads on the legs of a jack-up unit, loads on a group of conductors, and finally, wind forces on a semi-submersible unit. Potential problems in the use of wind tunnel modelling are also discussed and recommendations made for a unified and consistent approach to estimating mean drag forces on offshore structures.

INTRODUCTION

The estimation of fluid loading on offshore structures is fraught with uncertainties. But nonetheless, for conventional fixed structures taken as a whole, the overall design packages are generally considered to be adequate. Local elements can, and sometime do, fail, but whether this is a result of excessive fluid loading or due to other errors associated with fatigue assessment is unclear. In any event, to date, errors in fluid loading have never, so far as is known, resulted in any serious threat to the integrity of an offshore structure.

This is perhaps surprising when one considers that fluid loading data obtained from research studies consistently indicate force coefficients which are higher than those recommended and used in design (see eg Chakrabarti).[1] The generally accepted reason for this apparent inconsistency is that the process of estimating fluid loading on offshore structures is inherently conservative for both the static analysis and the fatigue assessment. As indicated in the Department of Energy's Proposed Guidance Notes,[2] for example, the following conservative assumptions are made:

1. waves are long crested (unidirectional);
2. water particle velocities are obtained from regular wave theories;
3. no shielding effects on the structure are included;
4. independent extreme values of wave and current are combined (extreme loading only).

In addition, fluid loading is in itself only part of a design chain which includes other safety factors (see eg Nataraja[3] of Lloyd's). Therefore, for conventional structures in conventional situations, it would appear that design procedures based on established practices are adequate (from a fluid loading viewpoint).

Over the past few years, however, research has steadily progressed towards a refinement in procedures for estimating both the static loads and the fatigue life of offshore structures. At the same time, there has also been steady improvement in the quality and relevance of force coefficient data for members of offshore structures. These data (see eg Bishop & Shipway[4] and Bearman *et al*[5]) are however still somewhat higher than those suggested by the guidelines and criteria. (Note however that the recent studies by Bearman *et al*[5] show that for smooth vertical cylinders the recommended values are close to those measured at postcritical Reynolds numbers.)

Any attempt to use a refined (and invariably more realistic) procedure in conjunction with the established, recommended values of force coefficients could seriously erode the safety margins inherent in current procedures. Fortunately, this is unlikely to happen for conventional structures, where the certifying authorities tend to insist on a more traditional approach.[2] The real danger, however, lies in unconventional structures, modifications to existing designs, mobile units, and 'one off' situations where either criteria do not exist or they are not entirely applicable. (Criteria exist for mobile units. However such criteria are not very rigorous, since such structures can in principle be moved to shelter. Current trends to extend the use of mobile units could invalidate some of the principles on which criteria for mobile units were founded.) In such cases, without the benefit of precedence, the most logical approach for designers is to use the most realistic loading assessment procedure, coupled with the best available data on force coefficients. The problem is then one of obtaining the most realistic force coefficients.

Dr Shyam Singh studied Aeronautical Engineering at Imperial College for his BSc and conducted research into fluid loading on bluff bodies for his PhD. He joined the National Maritime Institute (NMI) in 1979 and initially worked in research on wave forces on bluff bodies. He subsequently joined the industrial aerodynamics group and became heavily involved in the dispersion of gases. He left NMI in 1984 and after a brief spell at Wimpey Offshore he joined Noble Denton as a senior hydrodynamicist. He was subsequently promoted to Head of Hydrodynamics at Noble Denton but left to rejoin British Maritime Technology (BMT) (formerly NMI) in 1986. In January 1988 Dr Singh left BMT to work as an independent consultant engineer. In addition to fluid loading his recent efforts have been in environmental sciences, specialising in ventilation, dispersion of gases and related safety aspects for both land-based and offshore structures.

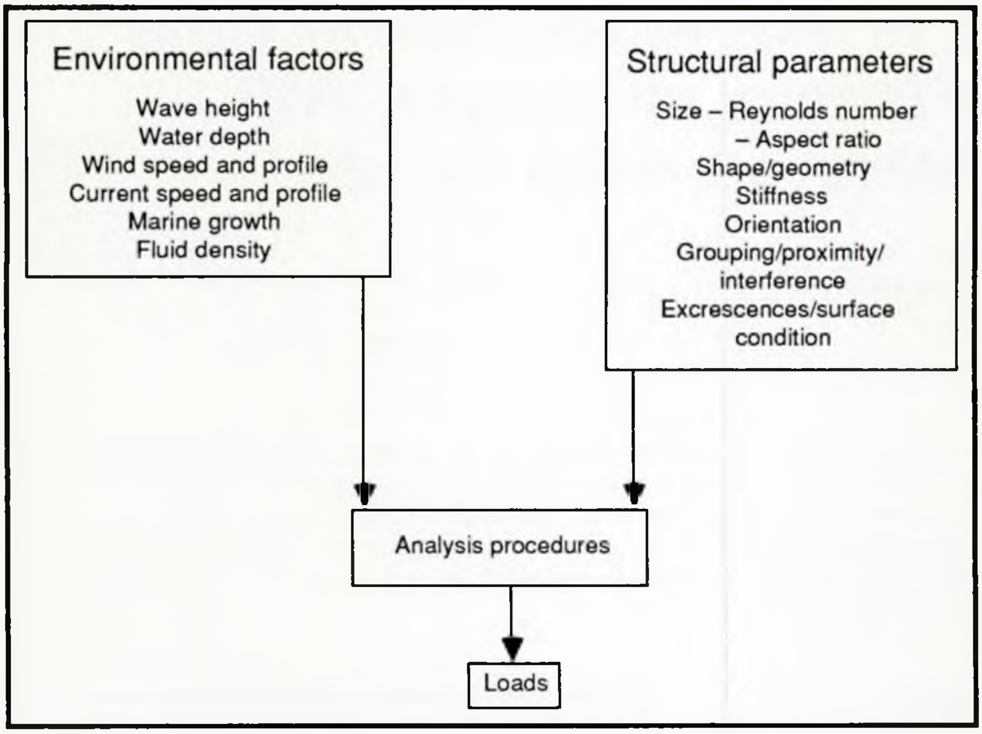


Fig 1: Routes to estimating fluid loading

This paper is written from the viewpoint of engineers and examines the difficulties and uncertainties in obtaining adequate data and procedures for estimating loads on offshore structures. Fluid loading covers a range of categories (diffraction, inertia, low frequency drift, drag), but this paper limits itself to steady (mean) drag forces. It is therefore relevant to high Keulegan–Carpenter wave flows (where the forces are dominated by drag and the flow can be assumed to be quasi-steady), mean current loading, and wind loading. [The Keulegan–Carpenter number is defined as UT/D where U is a characteristic flow velocity (usually the maximum), T is a characteristic period associated with the unsteady flow (say the wave period) and D is a characteristic bluff dimension of the body (say the diameter for a tubular member)]. It therefore gives a measure of the degree of flow development; eg high Keulegan–Carpenter numbers imply a quasi-steady flow.

Particular emphasis is given to wind loads since such loads are especially significant for mobile structures (since they affect mooring and hydrostatic stability and can augment motion-induced loads). Wind loads are also important for fixed structures during certain crucial phases, eg tow-out and installation. A reasonably accurate estimation of wind forces and moments is therefore essential to the design of offshore structures, particularly during transportation, but also because of the implications of operational restrictions, safety margins and overall design economy.

The paper firstly outlines some of the major parameters affecting drag loading with particular emphasis on wind loads. Possible difficulties and uncertainties in the estimation of drag forces are then illustrated by three case studies. These are:

1. forces on the legs of jack-up units
2. forces on groups of conductors
3. forces on semi-submersibles

Some of the procedures and pitfalls in physical model testing are then reviewed and recommendations for a way forward are presented.

FACTORS AFFECTING DRAG LOADING AND METHODS OF ASSESSMENT

Overview

From a fluid dynamic viewpoint, offshore structures are mostly complex three-dimensional bluff (ie unstreamlined) bodies, and can therefore attract much 'form' or pressure drag (as opposed to skin friction drag). The flow past such structures is not amenable to exact theoretical treatment (except for a few limited cases), and there are no exact methods for estimating drag forces on such structures. Recourse must therefore be made to empirical calculation methods or model tests in order to estimate these loads.

The commonly used calculation methods (eg refs 6–8) are generally straightforward with a high (but necessary) degree of empiricism. (Most of the predictive methods are essentially aimed at calculating wind loads, but can be used to estimate drag forces for currents or waves when appropriate.) The accuracy of these methods and the validity of the empirical factors is questionable and leads to uncertainty in the final result. Additionally, such methods are subject to misinterpretation and generally have insufficient guidelines or data to enable a confident and accurate estimation of the drag force on all but the simplest structures.

In principle the estimation of the drag force should be straightforward since the drag force can be obtained simply from:

$$F_D = C_D \, {}^1/_2 \, \rho \, U^2 A \tag{1}$$

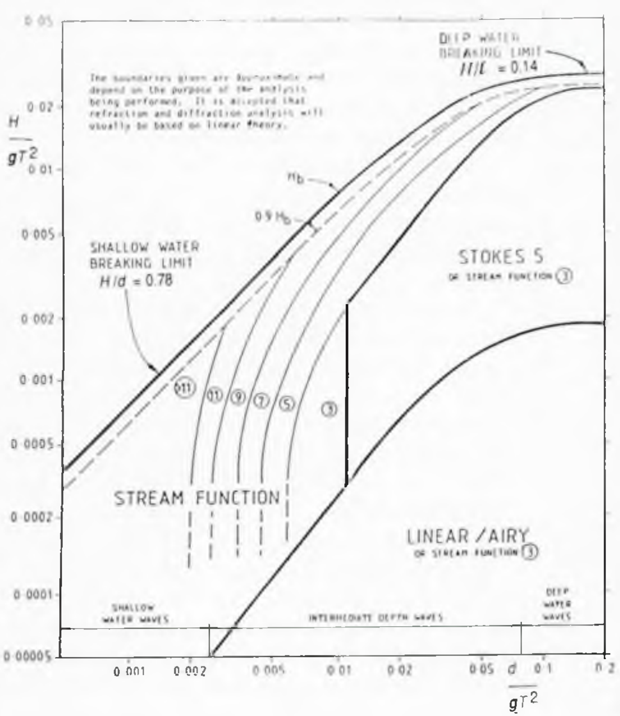
where F_D is the drag force, C_D is a drag coefficient, ρ is the density of the ambient fluid, U is the velocity normal to the axis of the body and A is a corresponding area. In practice, however, the estimation of drag is far from straightforward because of the dependence of the force on a number of parameters.

As shown in Fig 1, these parameters may be divided into two categories: those dependent on the external environment and hence which primarily affect the velocity (U) (and to a lesser extent the density, ρ) and those which affect the drag coefficient and the relevant bluff area (A).

Environmental parameters

It is apparent from eqn 1 that the estimation of the relevant velocity is an important step in determining drag forces. In waves, the water particle velocity is dependent on the wave height, wave period and water depth, and these parameters can be used to select the most appropriate wave theory (see eg Fig 2 taken from Department of Energy Guidelines). The above is relevant to regular waves; the position regarding irregular waves is less clear with additional uncertainties regarding the relevance of drag coefficients obtained from regular wave studies.

For loads due to currents, both the current speed and the



H/gT^2, dimensionless wave steepness; d/gT^2, dimensionless relative depth; H, wave height; H_b, breaking wave height; d, mean water depth; T, wave period; g, acceleration due to gravity.

The boundaries given are approximate and depend on the purpose of the analysis being performed. It is accepted that refraction and diffraction analysis will usually be based on linear theory.

Notes:

1. None of these theories is theoretically correct at the breaking limit.
2. Wave theories intended for limiting height waves should be referenced for waves higher than 0.9 H_b when stream function theory may underestimate the kinematics.
3. Stream function theory is satisfactory for wave loading calculations over the remaining range of regular waves. However, stream function programs may not produce a solution when applied to near breaking waves or deep water waves.
4. The order of stream function theory likely to be satisfactory is circled. Any solution obtained should be checked by comparison of the results with a higher order solution.
5. The error involved in using Airy theory outside its range of applicability is discussed in the background notes of Appendix C (ref 2).

Fig 2: Range of validity of wave theories (from ref 2)

profile are important. This was demonstrated in a recent sensitivity study[9,10] which concluded that for a North Sea jacket, the specification of the current was at least as important as the wave height and wave theory. Currents should therefore be specified in sufficient detail.

In prescribing the wind velocity for estimation of wind loads, uncertainties can arise firstly because of the selection of the averaging time (ie the gust factor) and secondly because of the degree of shear. Fig 3 shows the gust factors (at the 10 m height) for various averaging times as recommended by various authorities [eg American Bureau of Shipping (ABS)[6], Det Norske Veritas (DNV)[11], Department of Energy (D of E)[12] and British Standards Institute (BSI)[13]]. There is reasonable consistency between the values recommended by the various authorities. As Fig 3 indicates, it is common practice to use the 1 min mean wind speed for design loads on the complete structure, but shorter averaging times should be used for loads on components. ABS, for example, recommends that the 15 s gust should be used for broad 'block-type' elements such as living quarters, whereas the 3 s gust should be used for individual members and equipment on open decks. It is worth noting that for mobile offshore drilling units (MODUs) the 1 min mean wind speed is often used for all aspects of design and operations. Whilst the 1 min mean is acceptable for stability and mooring analyses, consideration should be given to using shorter averaging times when assessing local forces. This is especially relevant to sea fastenings and lashings for local elements during transportation.

Whilst there is general consistency between authorities with regard to gust factors, the same is not true for shear. Wind shear over the sea is usually characterised by a power law expression and inclusion of the gust factor then leads to the following expression:

$$\frac{U_{t(z)}}{U_{1h(10)}} = \alpha \left(\frac{z}{10}\right)^{\beta} \tag{2}$$

where α is the gust factor and is a function of height, β is the power law exponent, z is the height above mean water level, $U_{t(z)}$ is wind speed at z at an averaged t s, and $U_{1h(10)}$ is the wind speed at 10 m, averaged over 1 h.

Fig 4 shows the power law exponents as recommended by various authorities.[6,11,12,14] There is significant variation in the recommended values and they all differ from that obtained by Wills *et al*[15] from full-scale measurements over the sea. Note that because of changes in sea surface condition with wind speed, it would be reasonable to expect changes in the boundary layer profile. However, Wills *et al*[15] found that for wind speeds of up to 25 m/s, the power law exponent was apparently constant at 0.087. The impact of this power law on the velocity profile is illustrated in Fig 5, where the recommended values from the UK D of E[12] and DNV[11] are compared for the 15 s gust and 1 min mean. The greatest variation exists in the D of E values, where at 100 m, there is an implied 7% difference in velocity or a 15% difference in velocity squared (drag).

For MODUs, the effect of shear is often incorporated via a height coefficient (see eg ref 8). As Appendix II shows, however, the commonly used height coefficients are in fact based on a power law exponent of 0.105 (1/9.5). Further, and probably not widely known, is the fact that the height coefficient appears to have been derived using the velocity at 50 feet (15.24 m).

It is apparent from the above that much could be done to obtain a consistent approach to wind shear over the sea, and this could reduce the uncertainty in wind speed.

Structural parameters

As shown in Fig 1, and as mentioned earlier, there are a number of structural parameters which influence the drag coefficient and hence the drag force on elements of a structure. The size of the element is relevant because it determines the Reynolds number, the Keulegan–Carpenter number (for wave flows) and the aspect ratio.

For tubular elements or elements with rounded corners, the separation of the flow past the body and hence the drag coefficient is strongly influenced by the Reynolds number.

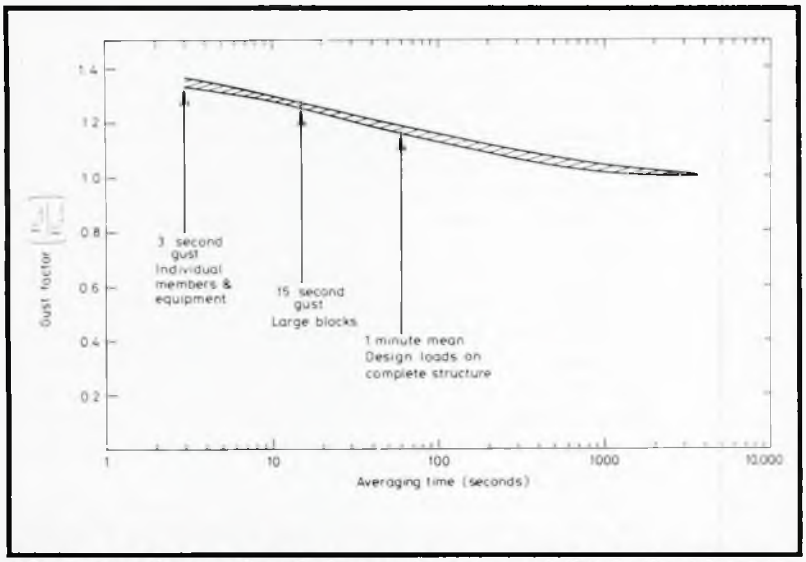


Fig 3: Gust factors for winds over the sea – relative to the 1 h mean wind at 10 m above sea level

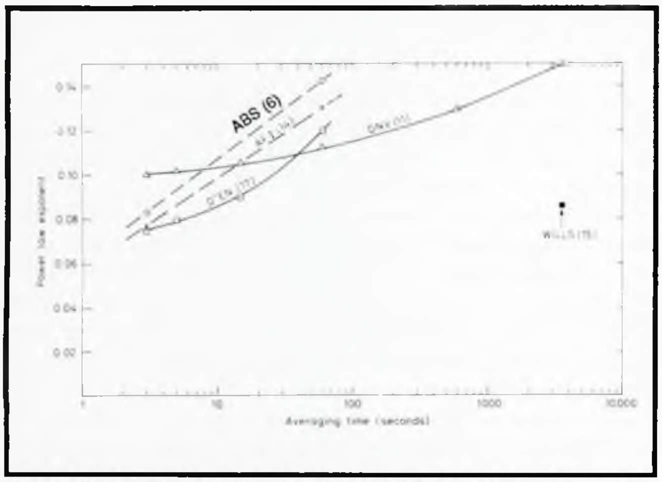


Fig 4: Recommended values for power law exponent for wind shear over the sea

Fig 6, taken from Pearcey *et al*[16], shows the classic variation of drag coefficient with Reynolds number for a smooth cylinder in steady flow. Similar data are available for other sections (see eg Delaney & Sorensen[17] and Aguirre & Boyce[18]). The Reynolds number for most components of offshore structures tends to be supercritical or postcritical in the design environment. Note however that data such as that shown in Fig 6 can rarely be used directly as many of the other factors, shown in Fig 1, alter the drag coefficient. The definition of the various flow regimes also vary with some of these parameters, and in waves the threshold of postcritical Reynolds number also varies with Keulegan–Carpenter number.[19]

The size of the body also influences the drag because the length-to-diameter (or width) ratio determines the aspect ratio and hence influences the two-dimensionality, or otherwise, of the flow. Fig 7 eg, taken from data in BSI CP3,[20] shows the influence of aspect ratio. Clearly, three-dimensionality in the flow causes notable reduction in drag coefficients but for jointed bodies other complex interactions occur at the joints or nodes. The shape or geometry of a body considerably influences the drag. In steady, unidirectional flow, data are available on a range of individual complex shapes,[17,18] but there are difficulties in applying such data to real structures because of interaction of various sizes, shapes etc. In waves, there is little data on non-circular bodies; this makes estimation of drag forces on pontoons of semi-submersibles difficult and uncertain. Basic data are available (see eg by Bearman *et al*[21,22]) but more data are needed for non-circular sections.

In unidirectional steady flow, the drag force on inclined bluff bodies can be reasonably estimated using the cross flow principle. This states that only the velocity normal to the axis of the body generates the drag.

In wave flows, the effect of body orientation is less clear. Whilst there are signs that a cylinder with its axis horizontal experiences less force than one with its axis vertical,[5,23] the influence of the orbit shape (ellipticity) is unclear. Bearman *et al*[5] suggest that at high Reynolds number there is little effect of ellipticity, whilst Rodenbusch & Gutierrez[24] reported noticeable influence of orbit shape. Guidelines and criteria make no distinction and carry little or no recommendations regarding body orientation.

Structural elements of offshore structures are never smooth and often have appendages of various forms. Offshore structures with elements in the splash zone and beneath the water surface attract marine growth. This takes the form of a hard accretion (barnacles, etc) and/or soft growth (seaweed). Miller[25] and Pearcey *et al*[23] discuss the effect of hard marine growth on the drag coefficient. This is illustrated in Fig 8 (taken from ref 23). Much less data are available on the effects of soft marine growth, but Pearcey *et al*[23] present some results showing the effect of simulated seaweed. As a comparison, it is suggested that whereas hard roughness with a relative height (k/D) of 5×10^{-3} increased the force by 11%, seaweed increased the force by some 40%. (This obviously depends on the type and distribution of the seaweed.) It must be stated however that the above was using simulated seaweed and further data are needed on the effects of such roughness.

Little data are available on the effect of appendages. The common practice is to allow for excrescences such as sacrificial anodes, ladders, etc, by increasing the bluff area. Work by Singh *et al*[26] has shown that for wave flows, this could underestimate the loads. Further data and guidelines for including the effect of excrescences are therefore required.

As mentioned above, most structures comprise a host of members of various sizes, and maybe shapes, and are often in close proximity. For wave loads, no mention is made of proximity effects such as shielding, but for wind forces, several of the authorities provide guidelines on shielding (eg refs 6, 8, 20). The effect of shielding is discussed further in the next section. Suffice is to say here that whereas shielding generally reduces the drag forces, other flow interferences can enhance drag.

Finally, the drag coefficient of a body is influenced indirectly by its stiffness. If the natural frequency of the body is close to the vortex shedding frequency, the possibility of vortex-induced vibration exists. The degree of vibration, and even the onset velocity, will be influenced by the amount of damping (fluid and structural) and should vibration occur, the drag coefficient will effectively be increased, depending on the amplitude of vibration.

CASE STUDIES

From the preceding section it is apparent that some data and methods are available to take account of most of the parameters

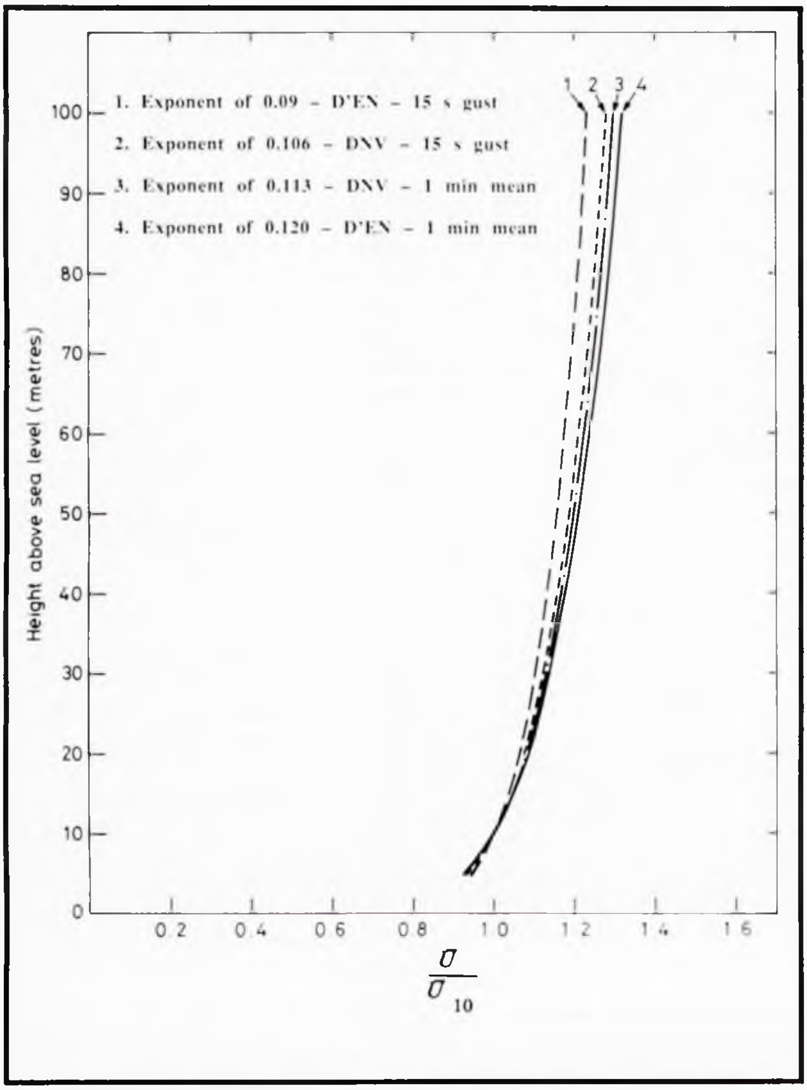


Fig 5: Profile of velocity over the sea for various power laws

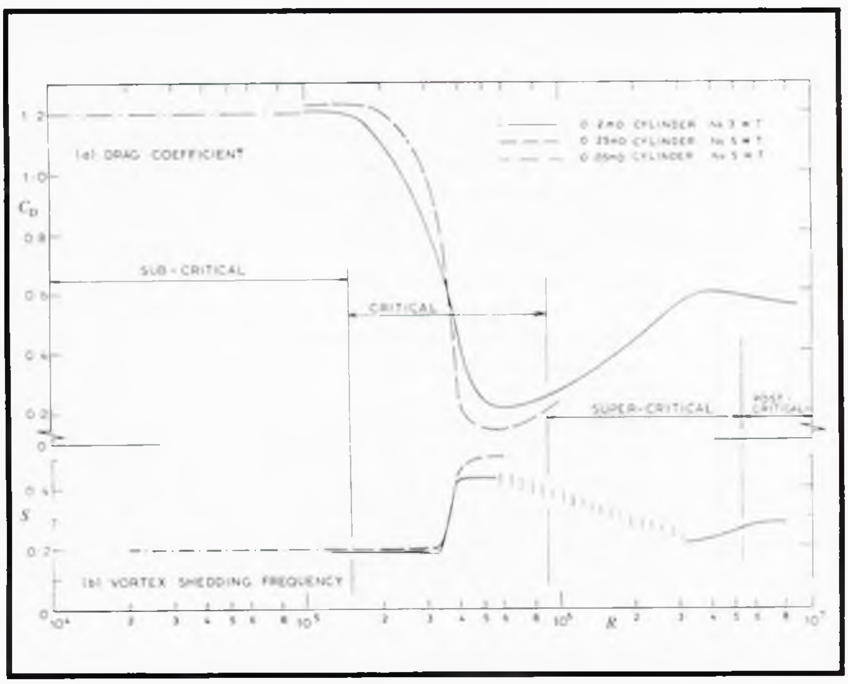


Fig 6: Variation of drag coefficient and vortex shedding frequency with Reynolds number (from ref 16)

that, individually, affect drag forces. Further data are nonetheless needed, and firmer guidelines on the way these parameters are taken into account are also required. However, the greatest difficulty is to assess the collective influence of these parameters relevant to real structures; and here, guidelines and data are almost totally lacking.

Three cases are presented in the following to illustrate the procedures and uncertainties present in the assessment of drag loads on some of the more complex structures.

Legs of jack-up units

Truss-type legs of jack-up units are among the most complex of items present on offshore structures. Such legs are often relatively long (100 m is not uncommon) and attract significant drag forces, both during transportation (when the legs are elevated and exposed to the wind) and during operations when currents and waves generate the forces. These legs (see eg Fig 9 taken from Pharr Smith *et al*[27]) comprise a number of tubular elements of various sizes, shapes and orientation, assembled in close proximity to each other.

The largest component of a leg is usually the corner post (or chord) for which there are various designs (Fig 10), but with a few exceptions, little data exists on the drag coefficient for corner posts. The exception is triangular corner posts where Pharr Smith *et al*[27] present wind tunnel data. Some data are also available for chords with symmetrical racks (Singh *et al*[26] and Ikeda & Tanaka[28]). Most of these data are however for relatively low Reynolds numbers; this may be acceptable for those flow angles where the body geometry is such that the flow separation points are fixed by sharp edges, but for other flow angles the data must be regarded as suspect. For cylindrical chords with symmetrical racks, the DNV rules for mobile units[29] suggest a simple procedure for estimating C_D *viz*:

$$C_D = C_{D_0} + 4\frac{\Delta}{D}\cos\alpha \qquad (3)$$

where C_{D_0} is the drag of the chord without the attachment, Δ is the mean height of the rack above the surface, D is the diameter and α is the angle of flow incidence. As Ikeda & Tanaka[28] show, however, eqn 3 underestimates the drag coefficient of the chord at small angles of incidence.

The estimation of loads on jack-up legs is treated in detail by Pharr Smith *et al.*[27] They have examined the various calculation methods and have compared the results with wind tunnel tests which included tests on the chords above, as well as on complete leg sections. It is apparent from their study that methods such as the BSI Lattice Tower Code,[29] where the structure as a whole is assessed, are likely to be more accurate than methods such as that given in ref 30 by DNV where efforts are made to take account of each individual member.

In this paper, only the Lattice Tower Code and the Marathon Marine Enginering Group

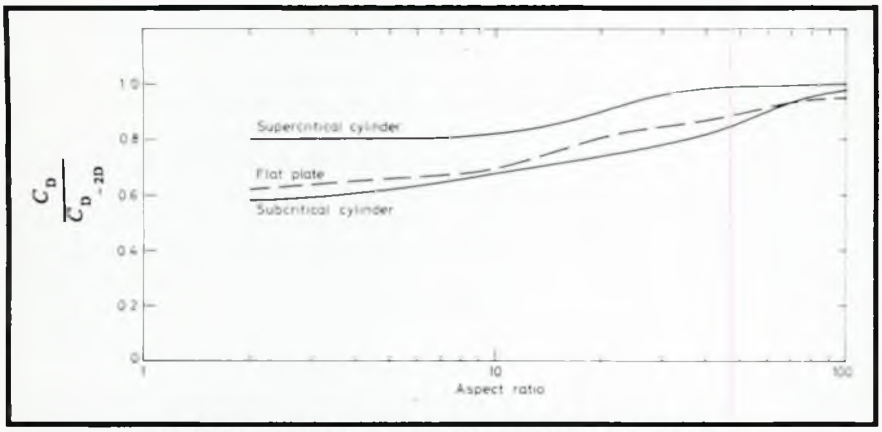


Fig 7: Effect of aspect ratio on drag coefficient

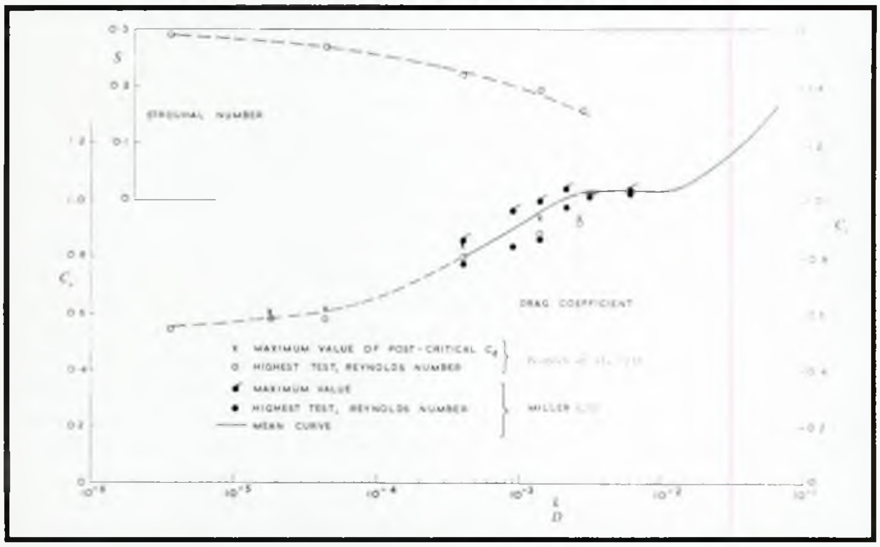


Fig 8: Variation of drag coefficient and vortex shedding frequency with relative roughness height (from ref 23)

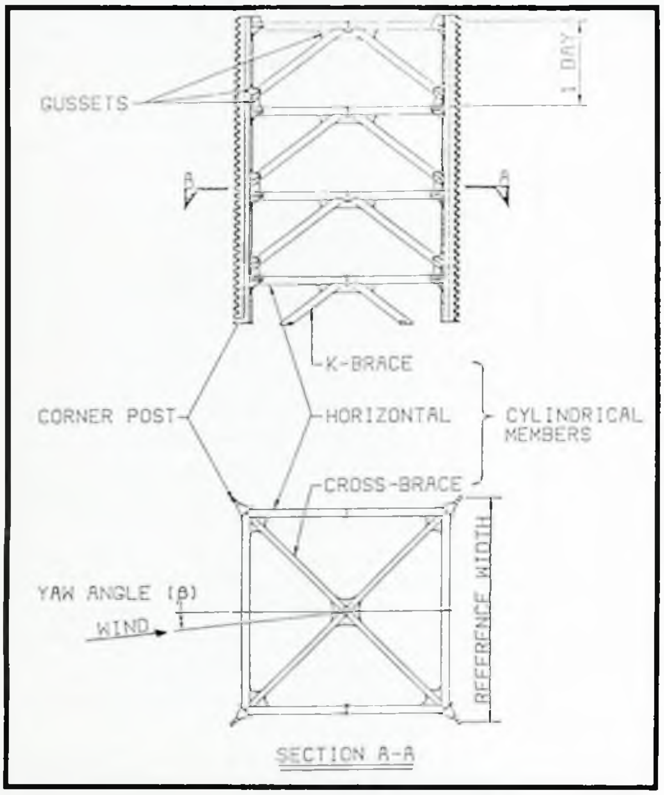


Fig 9: Typical leg section of a jack-up unit (from ref 27)

(MMEC) method (developed by Pharr Smith *et al*[27]) are examined. Procedures for using these methods are presented in Appendix I together with an example of the assessment of the drag on the leg of a Marathon 116-C class unit.

Briefly, the Lattice Tower Code was developed from extensive wind tunnel tests on various space frame structures. It works by representing the structure (in this case the truss leg) by an assembly of circular cylinders of subcritical Reynolds numbers, circular cylinders of supercritical Reynolds numbers and flat-faced members. The drag values of each group of members are then functions of the solidity ratio and empirical factors which depend on whether the leg is square or triangular. The effect of flow incidence is also taken into account using the solidity ratio and empirical factors. The limitation of the code lies in the fact that it was derived from members which were either circular or angled sections. Difficulties therefore arise with items such as corner posts which can behave as circular sections for some flow angles and flat plates for other angles.

The MMEC is similar in principle to the BSI Lattice Tower Code, with the solidity ratio again being an important parameter. One attraction of this method is that it enables the actual drag coefficient of each member to be employed and separates the effects of the windward and leeward faces. The disadvantage of this method is that it has been validated only for square legs. In addition, the flexibility of using actual drag coefficients can in itself give rise to errors by selecting inappropriate values.

Table I compares the results of the two calculation methods with measurements by Pharr Smith *et al*[27] and by Norton & Wolff.[31] The two calculation methods agree reasonably well and also compare well with the data of Pharr Smith *et al*. The comparison with the data of Norton & Wolff is not as good, but here the wind tunnel data may be suspect because of scale effects and methods used to overcome these effects.

Thus, based on the limited validation to date, both the BSI Lattice Tower Code and the MMEC method appear to provide reasonably accurate estimation of the drag of legs of jack-up units. These results, and the methods presented, are valid for members without marine growth or any appurtenances. Typical legs have anodes and other attachments such as ladders etc, and these will modify the results. The Lattice Tower Code has the facility for the inclusion of ancillary items, and marine growth can, as a first step, be included by increasing the size of members. It is evident therefore that further validation of these methods is required and the effect of marine growth etc should be examined. Since the methods suggested by the certifying authorities (eg ref 30) tend to be more inaccurate, it is recommended that methods such as the Lattice Tower Code be given further consideration.

Conductor groups

Conductors on fixed offshore structures are another case where complex flow patterns are generated and where the estimation of loads is difficult. These members can attract

Table I: Comparison of drag coefficients

Drag coefficients	*Calculations*		*Measurements*	
	BSI	*MMEC*	*Pharr Smith et al*[27]	*Norton & Wolff*[31]
At 0° yaw	0.66	0.67	0.67	0.59
At 45° yaw	0.82	0.81	0.79	–

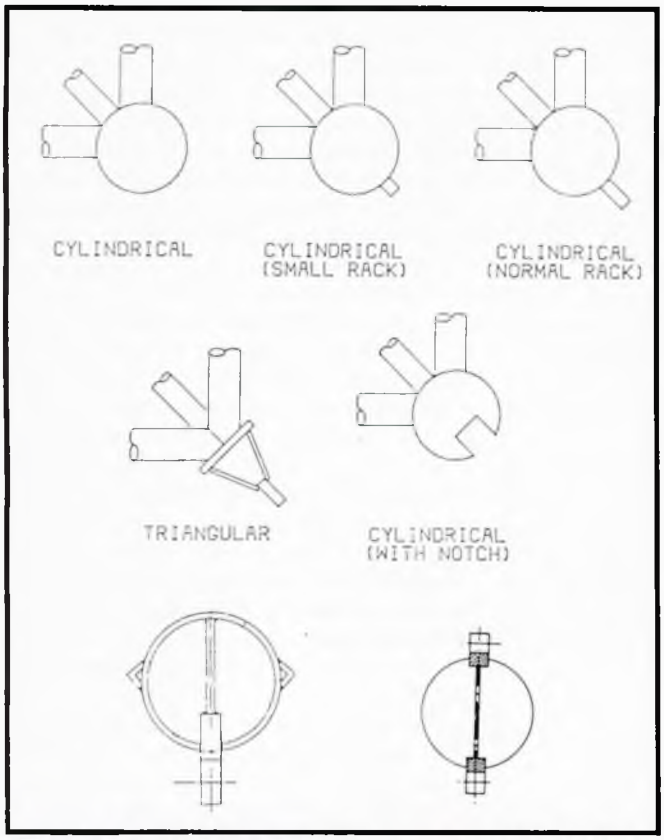


Fig 10: Range of possible corner posts on jack-up legs

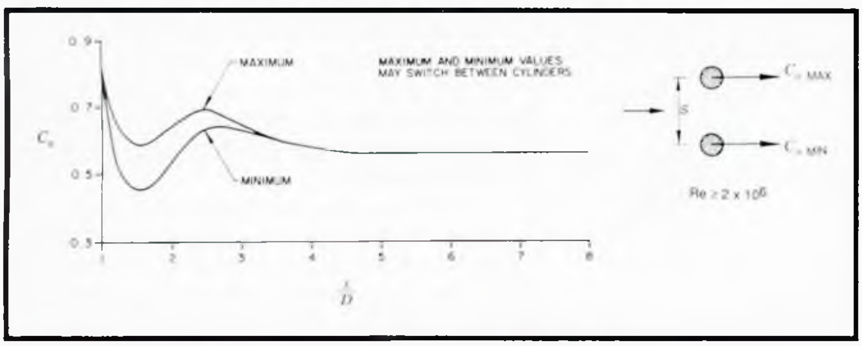


Fig 11: The effect of lateral spacing on the drag coefficient of cylinders in a side by side arrangement

significant loading and there have been rumours of cracks and other failures of conductors. Here again there is little guidance on the estimation of loads on conductor grouping. Incidentally, it should be noted that conductors are also prone to vortex-induced vibration because they are slender, but this can usually be 'designed out' by suitable support and structural stiffness. In this case study, only the static loads are examined.

The feature about groups of conductors that makes them stand out from other groups of members is the role of flow interference. This can be significant in the loading because the axes of members are parallel. Unlike lattice towers or jack-up legs therefore, flow interference of conductor groups is best dealt with on an individual basis. Much of the work on interference of circular cylinders has been summarised by the Engineering Sciences Data Unit (ESDU)[32]. It is evident that flow interference, and the degree of shielding, are dependent primarily on the spacing of the cylinders and their orientation relative to the flow. Fig 11 (taken from ESDU), for example, shows the effect of lateral spacing. Evidently for a spacing (centre to centre) of greater than 5 diameters, there is no flow interference and each member acts in isolation of the other. For cylinders in tandem, Fig 12 (from ESDU) shows that the downstream cylinder is shielded for distances in excess of 20 diameters downstream. The upstream cylinder can also be influenced.

The effect of interference on staggered cylinders is of more interest and relevance to practical structures. Figs 13 and 14 (plotted from ESDU data) show the effect of interference on the upwind and downstream cylinders respectively for a staggered pair. There is little influence on the upstream cylinder, but Fig 14 shows a strong effect on the downstream cylinder. It is interesting that, for flow angles of between 10 and 30°, the drag on the downstream cylinder increases, instead of being shielded. The fact that flow interference can cause an increase in drag is not new, yet shielding (ie reduction in drag) is often thought of as the only consequence of grouped members. Hence use of isolated values for each member is often thought of as being conservative.

The data presented in ref 32 (from ESDU) are only strictly valid for pairs of cylinders, but Pearcey *et al*[33] and Bushnell[34] performed tests on large groups of cylinders. Their results exhibited the same features as those shown in Figs 11 to 14. Fig 15, for example, from Pearcey *et al*[33], shows a reduction of drag for members in line, whereas at 18.5° flow angle, some members experience enhanced drag, resulting in an increase in the average drag. Whereas the increase in average drag may in itself be of lesser concern, the higher increase in drag of individual members could be significant.

Although the data from Pearcey *et al*[33] and Bushnell[34] are useful, it is insufficient to cover the wide range of groups and spacings present in conductors. The method proposed is therefore based on the data given by ESDU[32]. The main difficulty in applying the ESDU data to groups of cylinders lies in attempting to estimate the successive influence of upstream members on downstream elements. Fig 16 for example, produced from data by Pearcey *et al*[33], shows the progressive influence of shielding for five cylinders in line. It is evident that for each successive stage, the amount of shielding reduces; use of the ESDU data (Fig 12) could therefore overestimate the influence of shielding (ie underestimate the drag) for downstream cylinders in an array. This fact is therefore included in the procedure outlined below.

The conductor grouping examined consists of 32 conductors in two rectangular groups of 4 x 4, with a transverse spacing of 3.5 diameters and a longitudinal spacing of 4.5 diameters (Fig 17). Each conductor is assumed to have a diameter of 0.66 m (26 inches); therefore the Reynolds number would be ~1.2×10^6 for a 2 m/s current or wave velocity. Given that the conductors will experience some marine growth, postcritical Reynolds number conditions with an equivalent, isolated member drag coefficient of 0.7 is assumed (this is

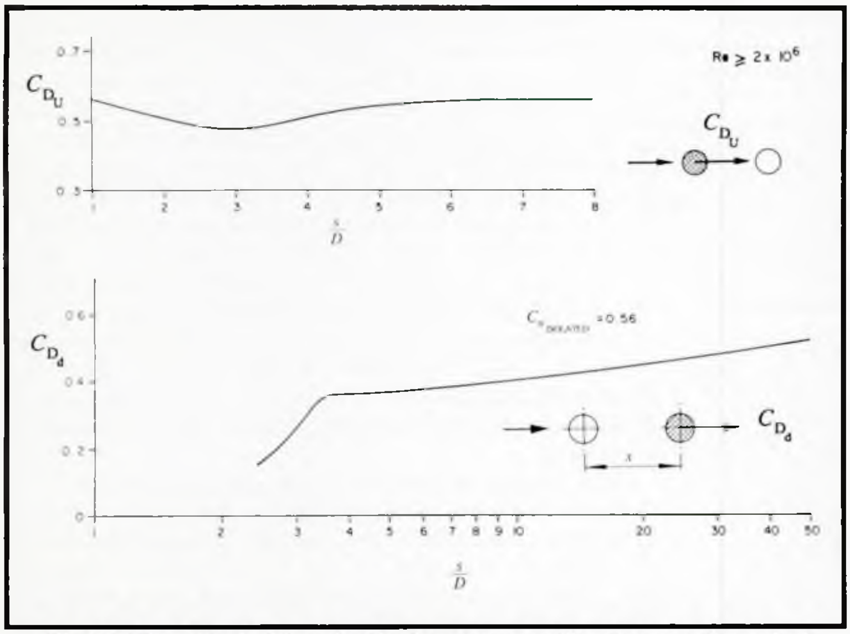


Fig 12: The effect of longitudinal spacing on the drag coefficient of cylinders in tandem

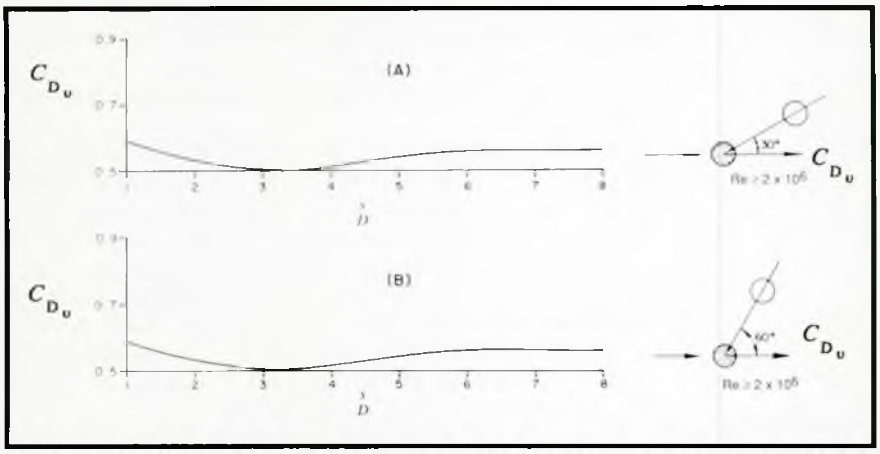


Fig 13: The effect of flow interference on the drag of upstream cylinders in a staggered arrangement

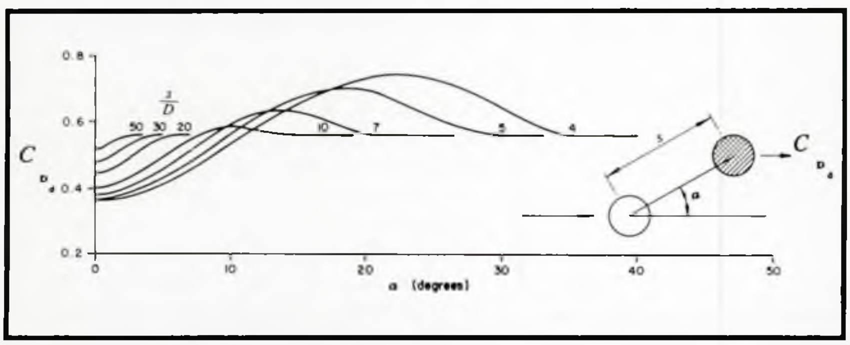


Fig 14: The effect of flow interference on the drag coefficient of downstream cylinders in a staggered arrangement

consistent with recommendations by certifying authorities, see eg ref 2).

For flow along the longitudinal axes, the first row experiences a small increase because of the proximity of each other in the transverse direction; Fig 11 for example gives ~4% increase for a spacing of 3.5 diameters. Fig 16 is then used to estimate the degree of shielding for each successive row, with no further decay after the 4th row (E). The resulting drag coefficients are then as shown in Fig 17(a). For flow from 90°, each row then acts independently, since the spacing normal to the flow is then 4.5 diameters, and according to Fig 11 will be unaffected by adjacent rows. Although Fig 16 was derived from data for in-line cylinders with 5 diameters spacing, these data can be used to assess the shielding of each column. The resulting drag coefficients are therefore as shown in Fig 17(b).

For oblique angles, eg from 45 deg (Fig 17c), the drag pattern is complicated. The derivation is based on examination of each individual member to see which upstream members affect it. For example, F2 is affected by the wakes of a number of upstream members but only G3 and G4 affect the drag. G3 for instance is 5.7 diameters away and makes an angle of 7 deg with the flow direction. The result is therefore a 10.7% reduction in drag (shielding) from Fig 14. G4, on the other hand, is 8.3 diameters away and makes an angle of ~12° with the flow; Fig 14 therefore suggests a 7.1% increase in drag. The final value of the drag coefficient is therefore:

$$0.7 \times 1.071 \times (1 - 0.107) = 0.67.$$

As a final example, the results for a flow angle of 115° are shown in Fig 17(d). This shows that local members can experience drag forces of up to 40% greater than the isolated values. Although this procedure is speculative, data by Pearcey *et al*[33] suggest increases of the order of 30% for individual members in a 3 x 3 array with 5 diameter spacing.

The final result for all flow directions is summarised in Fig 18. This shows that, compared with the isolated case, reductions of approximately 30% can occur for some directions, and increases of 25% may occur at some other angles. From a global viewpoint, for this conductor arrangement, taken as an average over all flow directions, the practice of using isolated member values (without shielding) would appear to be adequate. However, specific problems may be encountered for some flow directions and for individual members during these directions.

Although procedures such as this need to be validated, they represent a sound basis for estimating mean loads on groups of conductors where flow-induced vibration is not significant.

Semi-submersible wind loads

The estimation of wind loads on semi-submersibles is an important step in the design of such vessels because these forces affect the stability, mooring and station keeping of the vessel. Although much work has been carried out on wind loads on semi-submersibles (see eg refs 35–38), there still remains many uncertainties in predicting these loads, both from calculation procedures and from model tests. Incidentally, dynamic wind loads on semi-submersibles are also a source of concern as the low frequency components in the wind spectrum can excite and augment low frequency pitch and

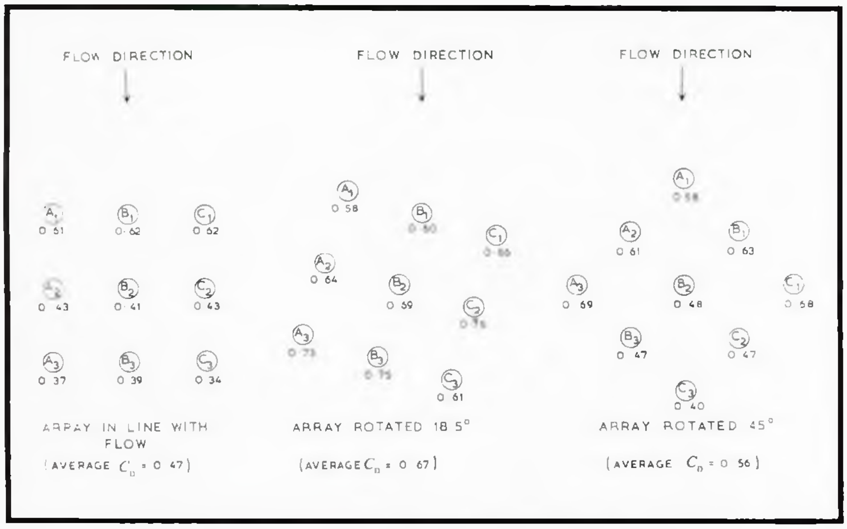


Fig 15: Drag coefficients on a group of cylinders (from ref 33)

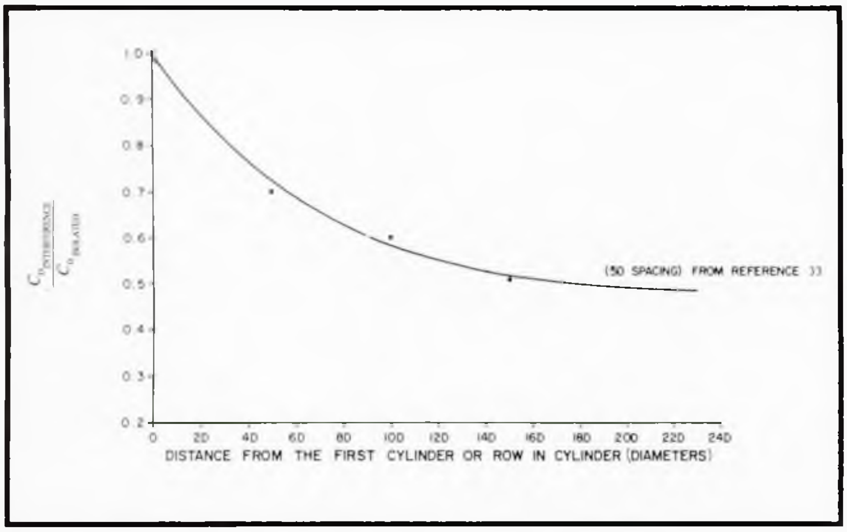


Fig 16: The effect of shielding on successive in-line cylinders

surge motions. In this paper however, attention is directed to mean loads only.

The calculation procedures commonly used, eg DNV[30] and ABS,[39] are based on a building block approach where the structure is split into discrete sections or elements; a projected area and a corresponding drag coefficient are then assigned to each element and the forces and moments on each are then given by:

$$F_i = 0.5\,\rho\,U_i^2\;C_{D_i}\,A_i \qquad (4)$$

$$M_i = F_i\,\bar{z}_i \qquad (5)$$

where ρ is the density of air, U_i is the wind velocity, C_{D_i} is the drag coefficient for the ith element and A_i is the area of the ith element and z_i is the height of the centroid of the element above the still water level. The total force and moments are then obtained by summing eqns 4 and 5 over all elements.

In the ABS[39] and IMO[8] methods, a reference velocity is used together with a height coefficient to incorporate the effect of shear. [The Environmental parameters section and Appendix II discuss an ambiguity regarding the height to be used for the reference velocity. It appears that this should be the velocity at 15.24 m (50 feet).] No shielding is allowed for columns. The DNV rules[30] assume a continuous variation of velocity with height by the use of a power law expression *viz*:

$$\frac{U}{U_{10}} = \left(\frac{z}{10}\right)^{0.09} \qquad (6)$$

Eqn 4 can then be rewritten for any given element as:

$$F_i = 0.5\,\rho\,C_{D_i}\,W\int_{z_1}^{z_2} U^2(z)\,dz \qquad (7)$$

where W is the width or diameter of the element, z is the height above the water line, and z_1 and z_2 are the heights of the bottom and top of the element respectively. The DNV rules also take account of the effect of aspect ratio (when it is less than 5) and the effect of shielding of columns when the spacing is less than 7 diameters.

In order to illustrate the effectiveness of these calculation procedures, the loads on a SEDCO 700 unit have been calculated and compared with wind tunnel results from Ponsford.[37] Calculations were performed for following and beam winds with the vessel at its operational draft (15.6 m air gap) and at level trim and even keel. The wind tunnel model tests were performed at a model scale of 1:150 in a boundary layer characterised by a power law with an exponent of 1/10, and the columns were fitted with trip wires to simulate high Reynolds number flow conditions. Figs 19(a) and 19(b) show a line drawing of the model tested; this was also the information used to perform the calculations. Note that the dimensions are in model scale metres.

The results of the calculations are summarised below and compared with the measured results in Table II. These are presented as percentage differences expressed as:

$$\Delta C_D = \left(\frac{\text{Calculated}\,C_D - \text{Measured}\,C_D}{\text{Measured}\,C_D}\right) \times 100$$

$$\Delta C_M = \left(\frac{\text{Calculated moment} - \text{Measured moment}}{\text{Measured moment}}\right) \times 100$$

With the exception of the moments in beam winds, the comparison is reasonable and within the usual expectations of accuracy for such methods.

These findings are consistent with those of Macha & Reid[38] who reported the results of a comprehensive study on wind loads on semi-submersibles. The main findings of that study were that the calculation methods predicted drag forces reasonably well when the vessel was at even keel. When the vessel was heeled, the comparison between measured and calculated values was less satisfactory. The moments were however, generally poorly predicted even for the vessel at even keel.

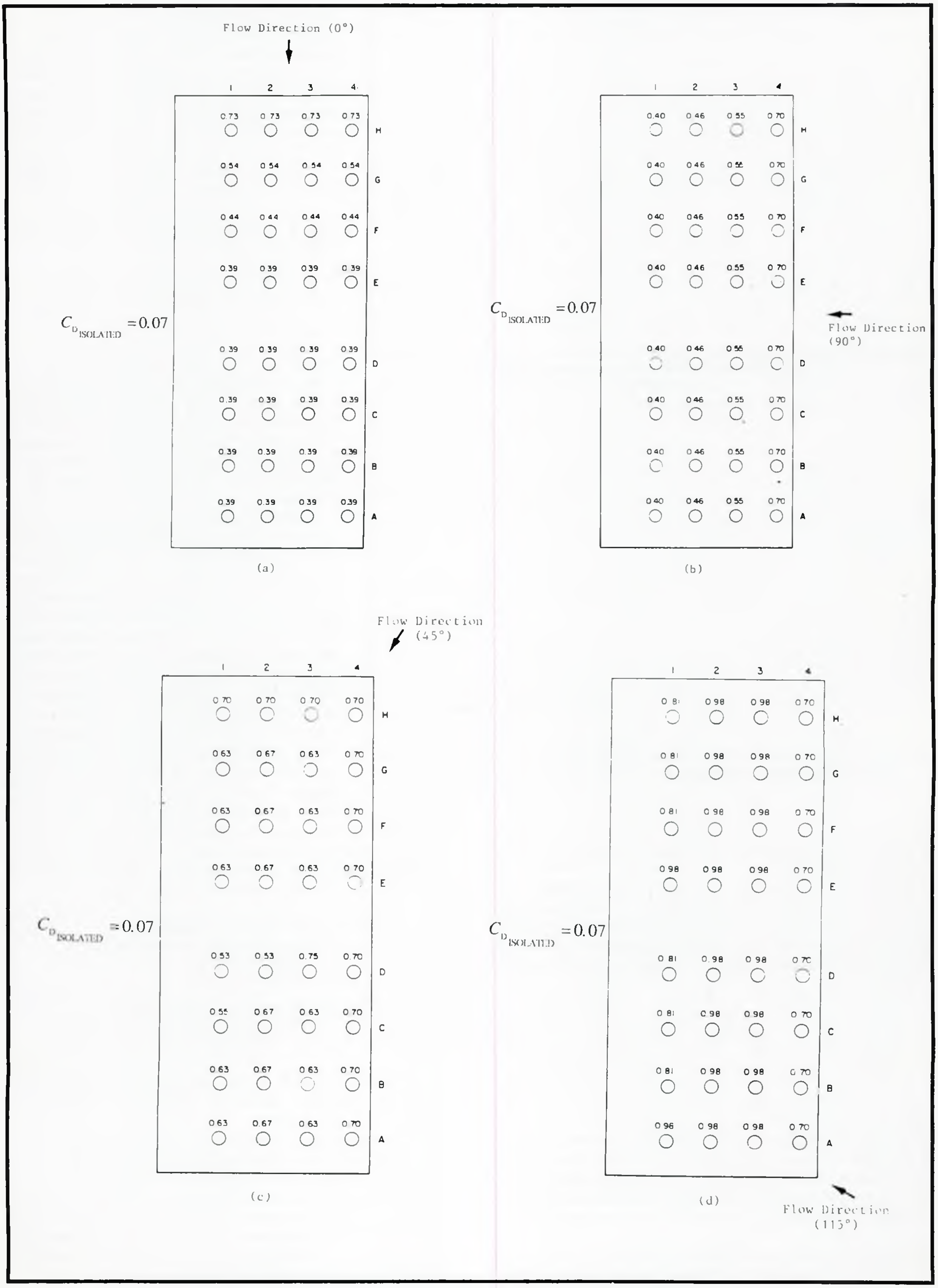


Fig 17: Calculated drag coefficients on conductors within a group

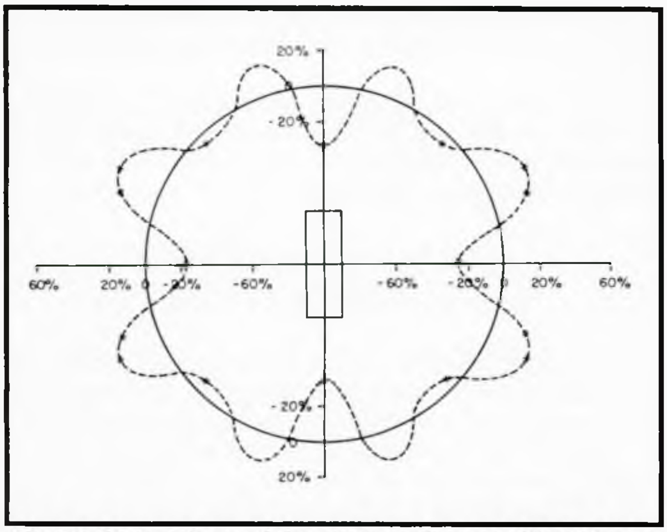


Fig 18: Comparison of the average drag coefficient on a group of conductors with that on an equivalent isolated cylinder

Table II: Drag coefficient differences

Wind direction	ΔC_D (%)		ΔC_M (%)	
	ABS	DNV	ABS	DNV
180°	–5	–1	14	1
90°	–1	–11	97	72

Macha & Reid[38] also pointed to the role of lift, shielding and other three-dimensional effects which affect the drag, and especially the moment, for the heeled vessel. They also point to potential problems in the use of wind tunnels as an alternative to estimating wind loads. In particular, for heeled vessels, problems can arise because of leakage of air under the columns where they penetrate the floor of the tunnel. The use of a flexible surface (liquid) to seal the area around the columns seems to be the most promising way ahead.

It is evident that an accurate estimation of wind loads on semi-submersibles is virtually impossible unless carefully controlled wind tunnel tests are carried out. The calculation methods will give reasonable predictions of drag at level trim and even keel but the uncertainty in moments will be high.

WIND TUNNEL MEASUREMENTS

The uncertainties in calculating wind loads on offshore structures has led to more use of wind tunnel testing to obtain reliable data. Indeed, a few years ago, the UK D of E commissioned a series of wind tunnel studies[40] which examined the problem of obtaining reliable data on wind forces on offshore structures. That study concluded, as did Macha & Reid's, that wind tunnel testing was the only viable way of obtaining reliable wind forces and moments on complex offshore structures. The alternative to wind tunnel tests and calculation methods is to perform full-scale measurements. But although this is feasible in some cases,[41] it is impractical as a design tool and expensive.

Wind tunnel testing is also impractical in the early stages of a design and should therefore be considered not as a

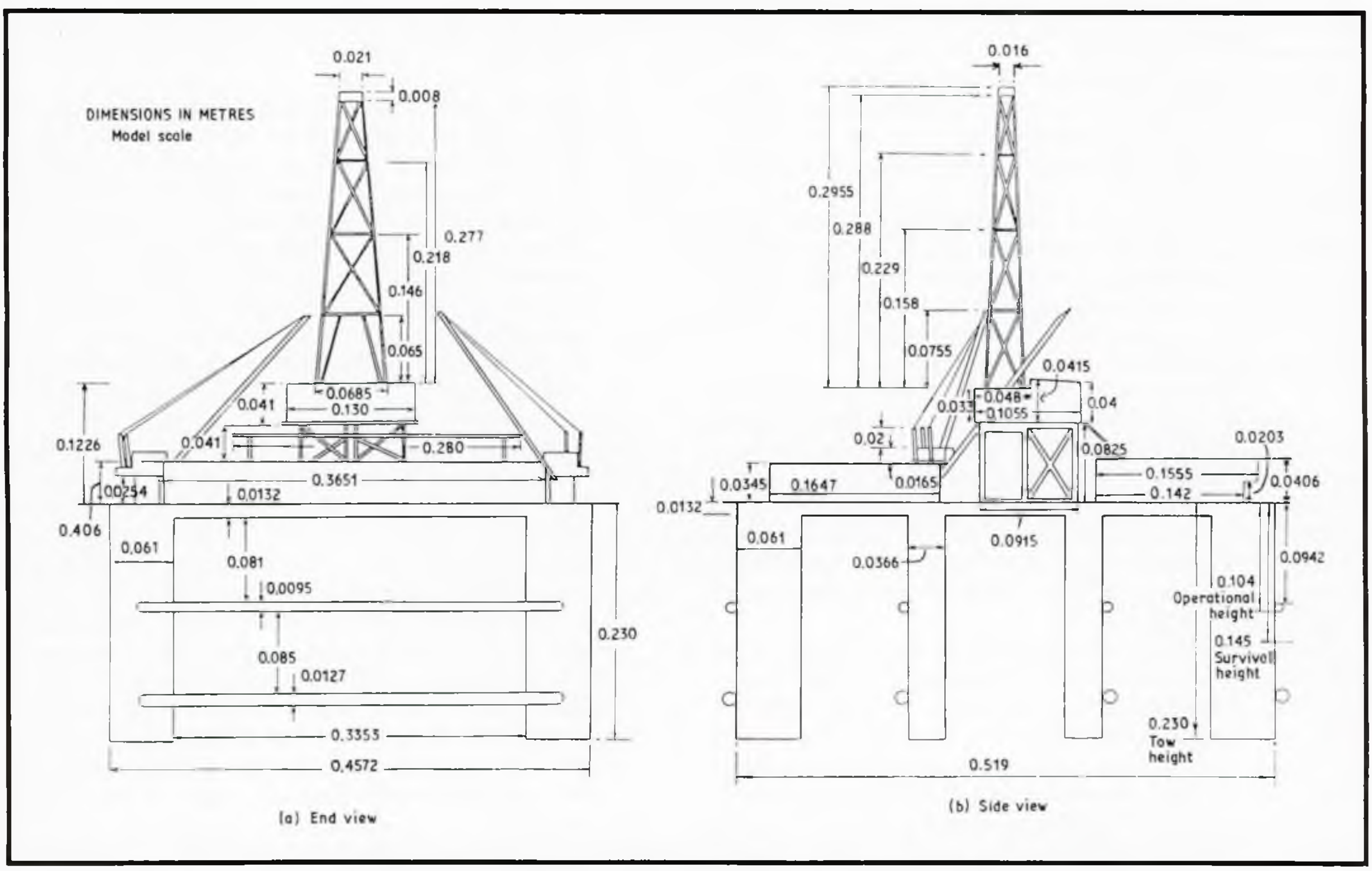


Fig 19: Line sketch of a SEDCO 700 used during model tests (from ref 37)

replacement for calculation methods but as a refinement which can be employed when nearing the final stages of a design. There are, however, certain limitations regarding what is practical and possible during wind tunnel tests.

In the earlier section, Factors affecting drag loading and methods of assessment, the main parameters affecting drag were discussed; these parameters must all be modelled or simulated appropriately during wind tunnel tests. Hence the profile of velocity and turbulence modelled in the tunnel should be typical of that over the sea. (Turbulence refers to the intensity of turbulence, but the scale of turbulence is also important as it affects the flow past, and hence the drag of, bluff bodies.) Wills *et al*[15] suggest that the velocity profile should be characterised either by a power law with an exponent of 0.087 or by a logarithmic law with a roughness length of 0.15 mm, and that the corresponding levels of turbulence intensity should be between 5 and 10%. As discussed, however, there is no unified approach to characterising the boundary layer over the sea. Hence wind tunnel studies should include provision for evaluation of the sensitivity of forces to variation in the boundary layer.

The main limitation and criticism of wind tunnel testing usually arises because of the inability to obtain sufficiently high Reynolds numbers. Scale models are usually built as large as possible, providing that the blockage of the flow is not too great; typically, it is recommended that the solid blockage (bluff area of the model/cross-sectional area of the tunnel) should be restricted to 5%, but even then corrections should be applied to take account of the blockage. In most wind tunnels, this invariably means that the Reynolds number for individual members is less than that at full-scale. This lack of similarity is only significant for bodies with rounded surfaces, where flow separation and wake structure will then be affected by Reynolds number.

Methods are available for simulating high Reynolds number flow conditions, and successful methods include the use of trip wires or roughness. Pearcey *et al*[16] for example, suggest that a roughness height of 5×10^{-5} times the diameter of the body will promote high Reynolds number flow at a lower Reynolds number without having any additional undesirable effects. This technique, as with some of the alternative methods, will only work providing other minimum Reynolds number criteria are met.

Turbulence may also be used to promote high Reynolds number flow conditions at lower Reynolds numbers. Such a technique should however be used with care as turbulence may alter the flow characteristics over other parts of a structure (such as block-type sections) and therefore be unrepresentative of prototype conditions.

For complete structures, the complexity of the flow past the structure makes it difficult to indicate *a priori* with confidence a critical Reynolds number above which tests should be conducted. The recommended approach is to perform sensitivity studies, but these should not be carried out on isolated elements of the structure. Instead, sensitivity studies should be carried out on the complete structure, by examining say the effect of adding fine roughness or trip wires to the surface of selected parts of the structure.

Another method of achieving high Reynolds number is to use a compressed air tunnel. The pressure and hence the density of the air surrounding the body is increased, and hence a higher Reynolds number can be achieved. Care should be taken however to ensure that compressibility effects are not introduced. Ideally the Mach number (wind speed/speed of sound) of the approach flow should be less than 0.2 and whilst it may be acceptable to perform tests at Mach numbers of up to 0.3, above this, corrections for compressibility should be taken into account.

Despite these limitations, the wind tunnel still is an invaluable tool for obtaining reliable force data on complex structures. This can however only be achieved providing some of the basic scaling and similarity parameters are satisfied.

CONCLUSIONS AND RECOMMENDATIONS

This paper has reviewed some of the major parameters affecting drag forces on offshore structures, particularly those due to wind, and has pointed out where some of these major uncertainties lie. Three case studies have been presented to illustrate some of the uncertainties that may arise in the estimation of drag forces. The main findings and recommendations from this work are as follows.

1. In prescribing wind speeds, although the recommendations regarding gust factors are largely consistent between the various authorities, the same is not true for shear. The biggest problem area appears to be for MODUs where the recommendations for gust factors are not clear and the implication appears to be to use the 1 min mean wind speed, even though the 15 s or even 3 s gust should be used for local items. Clearer and consistent guidelines are therefore needed.
2. There is inconsistency in the approach to fixed structures and mobile units in that different degrees of shear appear to be suggested for no apparent reason. Also, some codes for mobile units include the effect of shear via a height coefficient which appear to be based on a power law exponent of 0.105 and a reference height of 50 feet. Since velocity is conventionally presented with respect to the value at 10 m, if the height coefficient principle is to be used, the recommended values ought to be consistent with a reference height of 10 m as well as an appropriate and consistent exponent.
3. There is an abundance of data on isolated elements in uni-directional steady flow but little exists for complex structures comprised of simple shapes where interaction effects are studied. Little data also exists for wave flows for bodies of non-circular cross-section. Data of the effect of soft marine growth on drag forces are also needed. There are very few guidelines and recommendations for dealing with these problems. In addition, what little data or guidelines exist, appear to be valid mostly for cylinders of circular cross-section with their axes vertical. Guidelines for forces on horizontal and arbitrary inclined bodies are needed.
4. For loads on truss-type sections such as legs of jack-up units, methods based on assessment of the complete structure, rather than on the interaction of individual members, appear to be quite promising. It is recommended that methods such as the Lattice Tower Code be validated, and adopted by certifying authorities.
5. It has been demonstrated that the drag coefficient of individual members within grouped cylinders can experience an increased drag force for some angles of incidence. This fact, coupled with other data relevant to interference of cylinders, has been used to suggest a procedure for estimating the loads on individual members within conductor groups and hence on the complete group.

6. For semi-submersibles, existing calculation methods appear to predict the drag forces with reasonable accuracy when the vessel is at even keel. For other conditions, the estimation of drag forces is poor, possibly due to the fact that lift-induced forces and moments are not taken into account. Under all conditions, it appears that the prediction of overturning moments is unsatisfactory.
7. Wind tunnel measurements appear to be the only viable method for estimating loads on complex offshore structures. It is recommended however that these are used towards the latter stages of a design to refine and calibrate/validate the calculation methods.
8. There are limitations to what can be achieved in wind tunnels but, providing certain basic similarity requirements are met, wind tunnel techniques are available for obtaining reliable force data.

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APPENDIX I

METHODS OF CALCULATING DRAG LOADS ON LEGS OF JACK-UP UNITS

BSI Lattice Tower Code

The BSI Lattice Tower Code was derived from wind tunnel tests on various space frame structures and is applicable to towers of square or triangular cross-section. These can be uniform or tapered structures. The method takes account of the presence of ancillary items also but in this document only symmetrical towers without ancillary items will be presented.

Essentially, the drag coefficient of the structure is given by:

$$C_D = K_\theta \psi C_N \quad \text{(A2.1)}$$

$$C_N = C_{D_f}\frac{A_f}{A_F} + C_{D_c}\frac{A_{c'}}{A_F} + C_{D_c}\frac{A_c}{A_F} \quad \text{(A2.2)}$$

where: C_{D_f} = drag coefficient of legs composed of flat-sided members (can be used for corner posts);
C_{D_c} = drag coefficient of towers composed of circular subcritical members;
$C_{D_{c'}}$ = drag coefficient of towers composed of circular supercritical members.

These may be given by:

$$C_{D_f} = 1.76\,C_1(1 - C_2\psi + \psi^2) \quad \text{(A2.3)}$$

$$C_{D_c} = C_1(1 - C_2\psi) + (C_1 + 0.875)\psi^2 \quad \text{(A2.4)}$$

$$C_{D_{c'}} = 1.9 - \sqrt{[(1-\psi)(2.8 - 1.14\,C_1 + \psi)]} \quad \text{(A2.5)}$$

For square legs: C_1 = 2.25, C_2 = 1.5

For triangular legs: C_1 = 1.9, C_2 = 1.4

A_f is the projected area of flat-sided members in the face, when viewed normal to the face
A_c is the projected area of circular section members in the subcritical flow regime, in the face when viewed normal to it
$A_{c'}$ is as above for members in the supercritical flow regime
A_F is the total solid area = $A_f + A_c + A_{c'}$
ψ is the solidity ratio defined as A_F/outline area

NB – The BSI code should be consulted for a strict definition of the solidity ratio; in the above expression A_F is the solid area when viewed normal to the face in question.

$$K_\theta = 1.0 + K_1 K_2 \sin^2 2\theta$$

for square towers or

$$K_\theta = \frac{A_c + A_{c'}}{A_F} + \frac{A_f}{A_F}(1.0 - 0.1\sin^2 1.5\theta)$$

for triangular towers,

where

$$K_1 = \frac{0.55\,A_f}{A_F} + \frac{0.8\,(A_c + A_{c'})}{A_F}$$

K_2 = 0.2 for $0 < \psi < 0.2$ and $0.8 < \psi < 1.0$
= ψ for $0.2 < \psi < 0.5$
= $1 - \psi$ for $0.5 < \psi < 0.8$

θ is the angle of incidence of flow.

The MMEC method

The MMEC method was also derived from wind tunnel tests, but as stated earlier, is valid for legs of jack-up units only. The procedure, as given in ref 2, may be summarised as follows:

$$C_D = K_\beta \frac{\left(\overbrace{\sum A_i C_{d_i}}^{\text{Windward}} + \eta \overbrace{\sum A_j C_{d_j}}^{\text{Leeward}}\right)}{(L \cdot W)} \quad \text{(A2.6)}$$

where: K_β = $1.0 + 0.5728\,\psi(\sin 2\beta)^{0.9}$
η = $1.1 - C_{dwc}\psi$
ψ = $\sum A_i/(L{\cdot}W)$

(Note that ψ is again the solidity ratio.)

C_{dwc} = drag coefficient of the windward chord
A_i or A_j = component projected area of windward or leeward face
β = the angle of incidence (see ref 27 for detailed definition)
$L{\cdot}W$ = the outline area, L = eg bay height and W = eg the width
C_{d_i} or C_{d_j} = the drag coefficient of the components for the windward and leeward faces

NB One of the features of this method is that it allows the drag coefficient of the chords to be input so that if data are available for the chords a reasonable answer can be expected.

Example calculation: Marathon 116-C leg

As a demonstration of the application of the two methods described above consider the leg of a standard Marathon 116-C unit.

Step 1: Calculate the basic properties from the geometry. For this unit these are:

Leg type	116-C
Leg description	square, K-braced
Bay height (L)	3.41 m
Width of bay (W)	9.14 m
Outline (block) area ($L{\cdot}W$)	31.17 m^2
Projected areas:	
Chords	5.39 m^2
Gusset (A_f)	0.72 m^2
Horizontal bracing	2.44 m^2
Diagonal bracing	2.42 m^2
Total projected area	10.97 m^2 (This is the solid area A_F)
Therefore, solidity ratio (ψ)	10.97/31.17 = 0.352
Total circular area	4.86 m^2

Total flat plate area is the gusset area given above. The chord area must be considered as a separate category for the BSI method.

Measurements exist for the drag coefficients on the chords (only) of a 116-C leg. These are:

for windward chords	2.012
for leeward chords	1.625

Consider now the BSI method.

Step 2: Calculate the component drag coefficients using eqns A2.3 to A2.5.

For the flat plate (gusset) component, eqn A2.3 gives:

$$C_{D_f} = 2.36.$$

For the circular component, assuming that they are all supercritical, eqn A2.5 gives:

$$C_D = 1.283.$$

For the chords, we have two choices: either we assume the chords act as flat plates, in which case they have the same drag as the gussets (2.36) or we use the measured drag coefficient of 2.012, but a strict application of this method would take the chords as flat plates.

Using eqn A2.2 then gives:

$$C_N = \underset{\text{Gusset}}{2.36 \times (0.72/10.97)} + \underset{\text{Bracings}}{1.283 \times (4.86/10.97)} + \underset{\text{Chords}}{2.36 \times (5.39/10.97)}$$

Then for $\theta = 0$ degrees: $C_D = 1.0 \times \psi \times 1.883 = 0.664$. (Using the measured drag on the chords gives $C_D = 0.604$).

For $\theta = 45$ degrees, we obtain $K_\theta = 1.233$ then: $C_D = 1.233 \times 0.664 = 0.819$ for 45 degrees.

Using the MMEC method, then:

$$\eta = 1.1 - 2.012 \times 0.352 = 0.391 \text{ and}$$

$$C_D = K_\theta\ [(5.39 \times 2.012 + 0.72 \times 2 + 4.86 \times 0.7) + 0.391\ (5.39 \times 1.625 + 0.72 \times 2.0 + 4.86 \times 0.7)]\ /\ 31.17 = K_\theta \times 0.674$$

For $\beta = 0$ degrees $C_D = 0.674$.

For $\beta = 45$ degrees $K_\theta = 1.202$ so $C_D = 0.81$.

APPENDIX II

DERIVATION OF THE HEIGHT COEFFICIENT

The height coefficient (C_h) is often used as a means of incorporating the influence of shear in the atmospheric boundary layer into wind loading calculations. Consider for example a vertical bluff body of uniform cross-section; the drag force on an element is then given by:

$$dF_D = C_D\, 0.5\, \rho\, U^2(z)\, D\ dz \qquad \text{(A1.1)}$$

where C_D is the drag coefficient, ρ is the density of the ambient fluid, D is the transverse dimension, U is the far-field velocity and is a function of height, z.

The element between two heights, h_1 and h_2 would then experience a drag force by:

$$F_{D_{h_1-h_2}} = 0.5\, \rho \int_{h_1}^{h_2} C_D U^2(z)\, D\ dz \qquad \text{(A1.2)}$$

Using the height coefficient approach, the drag force on this portion of the body would then be:

$$F_w = C_D\, 0.5\, U^2_{REF}\ \rho\, A\, C_h \qquad \text{(A1.3)}$$

where U_{REF} is the reference velocity at some height, say z_{REF}, and A is the projected area = $(h_2 - h_1)\, D$.

Equating equations A1.2 and A1.3 then gives:

$$C_h = \frac{\int_{h_1}^{h_2} U^2(z)\, dz}{(h_2 - h_1)\, U^2_{REF}} \qquad \text{(A1.4)}$$

Writing the velocity profile as a power law with exponent $1/n$ then gives:

$$\frac{U(z)}{U_{REF}} = \left(\frac{z}{z_{REF}}\right)^{\frac{1}{n}} \qquad \text{(A1.5)}$$

which can be substituted onto eqn A1.4 and integrated to give:

$$C_h = \frac{\left[z^{(2/n+1)} \right]_{h_1}^{h_2}}{(2/n + 1)\ z_{REF}^{2/n}\ (h_2 - h_1)} \qquad \text{(A1.6)}$$

The height coefficient is therefore a function of the power law exponent ($1/n$) and the reference height implied in the velocity.

The best fit to the published values of height coefficients (see eg ref 8) appears to be for $n = 9.5$ and $z_{REF} = 15.24$ m (ie 50 feet). A power law exponent of 1/9.5 (ie 0.105) is consistent with the recommended values for fixed structures (Fig 5), but what is surprising is that the reference height appears to be 15.24 m (50 feet) instead of the 10 m commonly assumed.

Discussion

B W Smith (Flint & Neill Partnership) The author is to be congratulated on presenting a very comprehensive paper on fluid loading, which I found extremely interesting. I was particularly interested in the assessment of the appropriate wind structure and the derivation of drag forces on truss-type frameworks.

As consultant drafter of BS 8100, the Lattice Tower Code, I was pleased to note Dr Singh's comments on its suitability, subject to further calibration, for assessing the drag of offshore structures of truss form. Results of such calibration tests on land-based structures since publication of the Code have given us more confidence that it does predict drag forces reasonably accurately, and certainly better than other existing documents. It has, in fact, been adopted in modified forms, for several international documents on lattice-type structures. It can also deal with the mixed form of construction mentioned by the author by building up the frame element by element.

BS 8100, however, was written for land-based structures and as such is not generally applicable to the design of platform-mounted towers or booms offshore. This was recognised by the Department of Energy in 1980 who commissioned us to extend the draft code (as it was then) to make it applicable to offshore structures. With publication of the Standard in 1986, the Department requested us to update our report on this work, to be compatible with the published Code, and we shall shortly be submitting our final report to them. This will include a separate report which provides guidance notes which can be read in conjunction with BS 8100 to enable offshore-mounted lattice towers to be designed on a rational basis.

The principal amendments necessary to the Code are:

1. adjustment of the wind structure to take account of the relevant sea state conditions.
2. assessment of the interference effects of the platform on the wind flow on the tower.
3. extension of gust factors to take account of the relevant sea state conditions.
4. extension of the Code provisions to deal with inclined booms supported by struts or ties along their length.

I would value Dr Singh's comments on these four aspects. He has discussed item 1, the adjustment of the wind structure, in his paper but has limited his comments to published design rules quoting short duration gust speeds. The Lattice Tower Code, and indeed the revision to CP3, Chapter V, Part 2, presently being drafted and with which I am involved, uses the hourly mean wind speed as the basic reference speed and appropriate gust response factors are then calculated according to both the environment and the structure's response. In the Tower Code this is a single factor and in the revision to CP3 (to be published as BS 6399, Part 2) it will effectively be two factors, one to derive the gust speed and the second a dynamic magnification factor to account for the response of the structure. Clearly, for the majority of conventional buildings, that factor will be unity. I would be interested in hearing Dr Singh's views on the necessary response factor that may be required for offshore structures. For certain boom structures, for example, I believe this could be significant.

With regard to interference effects of platforms on wind flow, it is rather academic, I believe, to assess the power law index (or appropriate log law) for variation of wind speed with height, very accurately, when obstruction of the platform has such a significant effect on this variation. In the guidance notes we have drafted, we have produced an empirical formulation to account for such effects and compared the results with wind tunnel tests. Again I would be interested in Dr Singh's comments and experience on this aspect, although this would clearly not affect drag on platforms in transit or conductor loads. It could be significant, however, in the overall loading of the semi-submersible, when account is taken of the wind loads on the superstructure.

With regard to inclined booms, a shortfall in the Lattice Tower Code is the prediction of drag for inclined frames, which is also recognised by Dr Singh. This has been covered to some degree in the work undertaken by BRE on cranes, but not sufficiently for tubular members. I have tried to cover this aspect in the guidance notes based on the cross-flow principle used also by the author, but we did not undertake any further wind tunnel tests to verify our proposals. The problems of simulating supercritical flow in the wind tunnel still leave me with some concern and I believe that more full-scale information would help. I note Dr Singh's comments in his conclusions, however, that he believes wind tunnel tests are the only viable methods for estimating loads. Unfortunately such tests are not always feasible, nor do they always meet the constraints that he mentions in his paper. Tests in supercritical or mixed flow seem to me to be essential before we can give greater confidence to our design rules, but the facilities for such tests are not practically available to the engineer, and the simulating methods suggested appear to me to introduce further uncertainties. Finally, as the title of the paper implies, we are dealing with uncertainties and, from a code drafter's point of view, we need to quantify these uncertainties in order to achieve the required reliability and safety in the design process that the author mentions in his Introduction. May I make a plea, particularly to the wind tunnel testers, that they try to assess and present the uncertainties in their results to assist in this process?

S Singh (BMT) I am pleased to hear Mr Smith's comments regarding further calibration of the Lattice Tower Code for land-based truss structures and that the Code will be extended to include offshore-mounted structures such as towers and booms. Unfortunately, I have never tried to use the Code to assess wind loads on smaller (but nonetheless important) elements of offshore structures such as booms and towers, and I certainly agree that for calculating the individual loads on such components, the response of that element should be taken into account.

For loads on the complete structure (ie which comprises all the smaller elements such as booms, towers, derricks etc) it may not be necessary to pay as much attention to the dynamic characteristics of each individual element. This brings me to another point raised by Mr Smith, namely that of the wind flow over the platform. When assessing the loads on the complete structure it is appropriate and highly relevant to consider the wind flow approaching the structure. This is particularly important in model tests where the shear can alter the forces by significant amounts depending on the orientation of the structure. I agree, however, that for assessment of local loads, ie the loads on smaller individual elements of a platform, the local flow will be governed by the flow over the superstructure and by the wakes from the surrounding elements. For such cases shear is less important than say the intensities of turbulence, the appropriate averaging period for the velocity (ie the gust factor) and the response of the (local) structure. It would still be appropriate, however, to attempt to derive an appropriate velocity which took into account the approaching shear as well as the possible influence of the surrounding structure.

With regard to the comments concerning wind tunnel tests, again I agree that there will be occasions when tests are not feasible because the minimum scaling and similarity requirements cannot be met. As I mentioned in the paper, even when the scaling requirements appear to be satisfied sensitivity studies would be useful as they would give greater confidence in the results from model tests, as well as provide data on uncertainties in the tests.

P A Frieze (Advanced Mechanics and Engineering Ltd) Like Dr Singh, I am interested in the uncertainties associated with environmental forces but with application to structural reliability analysis. For this the uncertainties need to be quantified in probabilistic terms involving bias, coefficient of variation (COV) and distribution type. Bias is a measure of the mean value of actual results relative to predicted results whereas COV indicates the extent of the spread of results around the mean value. Distribution type is important once calculations to determine safety levels or margins are to be executed.

I recently had to determine the probabilistic parameters for steady wind forces at sea for use in a mean level II reliability analysis technique. Data reported in refs 1 and 2 below seemed to be precisely what was needed. They related wind speed measurements recorded on a semi-submersible to the total forces on the unit, as determined from the relevant components of tensometer readings from all moorings. The results were compared with Code predictions and with specifically executed wind tunnel tests. Biases were simple to ascertain; wind tunnel tests ÷ Code predictions = 0.75, and full-scale measurements ÷ wind tunnel tests = 0.75 or 0.68, depending on whether the results related to a 1 h mean wind speed or a 1 min sustained speed – the references are completely non-committal as to which is the more appropriate.

Determination of the corresponding COVs and distributions was less easy, as illustrated in Figs 1–3 below.

These results pertain to three wind speed ranges, here labelled A, B and C, details of which can be found in ref 3 below. In seeking to determine the distribution type, a log-normal distribution was preferred since it was the type assumed in the mean level II reliability analysis technique to be adopted in the work. For data sets A and B this proved to be a reasonable approximation (see Figs 1 and 2).

However, as Fig 3 shows, a log-normal approximation to data set C is quite inappropriate because of the clearly double-peaked nature of the results. Despite this, it was found possible to approximate data set C by a pair of log-normal distributions on the basis that current had been ignored when the initial measurements were taken (see ref 3 below). An average distribution with a COV of 0.225 was determined as appropriate for use in the reliability calculations.

The question raised by this investigation is: why are Code steady state wind force estimations so much greater than both wind tunnel results and full-scale data? I have myself surmised, and I would like it confirmed if possible, that part of the answer lies in the distinction between steady state and low frequency wind forces. I postulate this based on the recent paper by Ochi & Shin (ref 4 below) which examines a number of wind spectra measured at sea and finds the energy in the low frequency range to be considerably greater than that estimated by all the normally used spectra because they are based on measurements conducted over land. Is it possible that recording techniques and/or their interpretation make it difficult to distinguish between low frequency and steady state forces, and tend to lump it all within the latter?

I would welcome Dr Singh's views on these matters, and congratulate him on his presentation of a difficult subject.

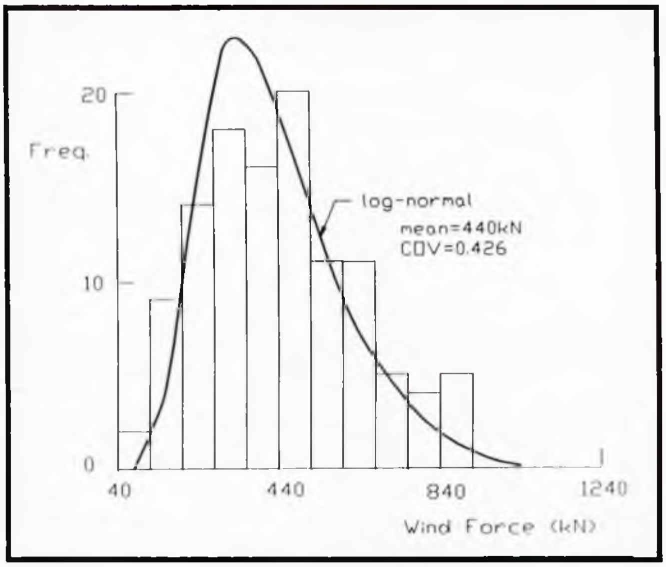

Fig 1: Data set A

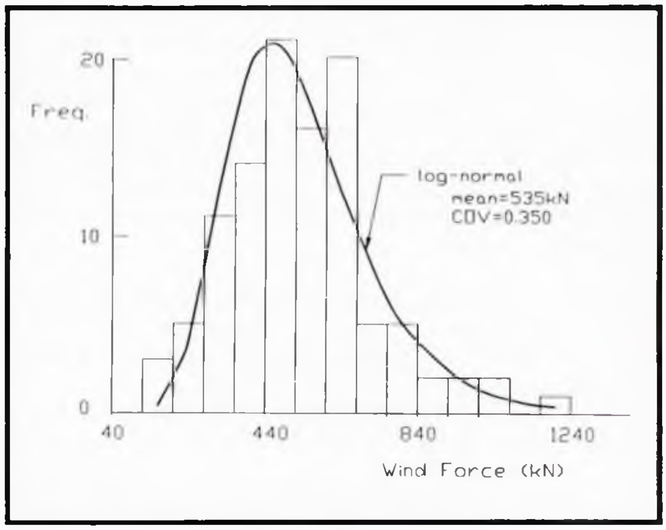

Fig 2: Data set B

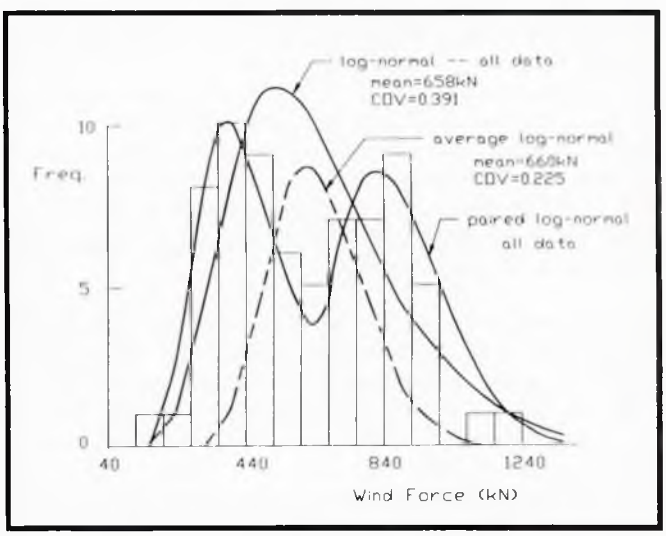

Fig 3: Data set C

References

1. H Boonstra, 'Analysis of full-scale wind forces on a semi-submersible platform using operator's data', Offshore Technology Conference, Paper OTC 3628 (1979).
2. H Boonstra & C Leynse, 'Wind tunnel tests on a model of a semi-submersible platform and comparison of the results with full-scale data', Offshore Technology Conference, Paper OTC 4245 (1982).
3. P A Frieze, 'Wind forces on offshore structures', Proc Intl Conf Floating Production Systems for the 1990s, RINA, London (1988).
4. M K Ochi & Y S Shin, 'Wind turbulent spectra for design consideration of offshore structures', Offshore Technology Conference, pp 461–467 (1988).

S Singh (BMT) The questions raised by Dr Frieze are indeed interesting. Firstly however it must be noted that the data presented by Dr Frieze represent one series of measurements only and that his findings may not necessarily be true for other units.

In response to the question regarding the lack of agreement between the full-scale measurements, the wind tunnel tests and the Code, it should be noted that full-scale measurements are subject to a number of practical difficulties and the uncertainties in such measurements could be high. Indeed Dr Frieze points to one omission during the full-scale measurements (that of the effect of currents). I would however support Dr Frieze's view that low frequency energy in the wind spectrum could be one cause for the lack of agreement between the various methods. The paper discusses reasons why the Codes can produce misleading results but I would expect a well-designed wind tunnel test programme to produce results which would be closer to the full-scale measurements than those presented by Dr Frieze; more comparisons of this sort are therefore needed before questions such as these can be properly resolved.

S J Rowe (BMT Fluid Mechanics Ltd) In Table II the author has pointed out the very large errors that can occur in overturning moments if the DNV or ABS wind load calculation methods are used.

The chief merit of these methods is that they are relatively simple and easy to apply. In his use of these methods, has the author considered any potential improvements to the methods, say to introduce the influence of lift forces on moments, or does he feel that the present methods are incapable of such improvement?

S Singh (BMT) Regarding Mr Rowe's question in connection with possible improvements to the simple wind loading calculation methods, I feel that there is little virtue in trying to improve these methods. As mentioned in the paper the main reasons for the failure of such methods (for some situations) appears to be the result of three-dimensional effects, lift forces and shielding which are not taken into account. Such effects cannot be easily incorporated into the present structure of these 'building block' type methods.

M M Zdravkovich (University of Salford) The author should be complimented on the comprehensive cover of two closely related, but still insufficiently appreciated, aspects of fluid loading – the uncertainty and interference effects. The latter were amply discussed and described in Figs 11–18 of the paper.

The effect of lateral spacing on the drag coefficient of cylinders in a side by side arrangement is shown in Fig 11. The effect of the lateral spacing on the lift coefficient is not shown. In the subcritical flow régime the value is less than half that of the drag. At higher Reynolds numbers, which are more relevant for offshore applications, the effect of interference leads to a phenomenal increase in the lift coefficient. ESDU 84015 (ref 32 of the paper) states 'no data were found for the lift coefficient in supercritical flow but the data for subcritical flow may be expected to overestimate'. The latter statement is incorrect and dangerously misleading for designers. The actual value of the lift coefficient can exceed 2.1 at $Re = 3.4 \times 10^5$ and $s/d = 1.2$ (ref 1 below).

The reason for this unexpected trend lies in the formation of separation bubbles. In the case of a single cylinder, the separation bubble suddenly appears on one side and the resulting asymmetric flow causes a lift coefficient of 1.2 (ref 1 below). For two cylinders arranged side by side, the stagnation points are displaced towards the gap and the bubbles always appear on the sides away from the gap. The flow asymmetry is enhanced by the reduced velocity in the gap and the lift coefficient exceeds 2. The variation of lift coefficients on both cylinders in terms of the spacing and Reynolds number have been measured by Okajima & Sugitani (see ref 2 below). This is an example of how the extrapolation coupled with uncertainty produced a serious underestimate of fluid loading.

Another example of uncertainty of interference effects is in loading of conductor groups. The importance of flow direction and the location of each individual conductor is amply described in Figs 15 to 18 in the paper. Only drag coefficients on each individual conductor are measured; however, there is also a lift force to consider. For example, if the spacing of a 6 x 3 group of conductors is 2 and the flow direction is 18 deg, the measured $C_L = 0.81$ and $C_D = 0.48$ on the conductor in the second row (ref 3 below). The lift coefficient becomes the dominant one. This area seems to have been overlooked by researchers.

The final example is the interference effect caused by the intersection of structural members. The effect was ignored because it was expected that fluid loading should be reduced in comparison with the uninterrupted two-dimensional member. However, a complex three-dimensional wake produced pairs of counter-rotating streamwise vortices which induced local additional loading in excess of the corresponding two-dimensional case (ref 4 below). Hence, an *a priori* assumption removed uncertainty and replaced it with ignorance.

References

1. P W Bearman, 'On vortex shedding from a circular cylinder in the critical Reynolds numbers regions', *Journal of Fluid Mechanics*, Vol 37, pp 577–587 (1969).
2. M M Zdravkovich, 'The effects of interference between circular cylinders in cross flow', *Journal of Fluids and Structures*, Vol 1, pp 239–261 (1987).
3. M M Zdravkovich, J A Nuttall & D M Causon, 'Flow-induced vibration in staggered tube banks', 6th Thermo and Fluid Mechanics Convention, Inst Mech Eng, pp 237–243 (1976).
4. M M Zdravkovich, 'Flow around two intersecting circular cylinders', *Journal of Fluid Engineering*, Vol 107, pp 507–511 (1985).

S Singh (BMT) I must thank Dr Zdravkovich for raising the point about lift forces. In the paper I concentrated on the in-line drag component of force simply because this is often the component of interest. I do however accept that lift forces are often underestimated even though this component can (as Dr Zdravkovich points out) even exceed the drag component under certain circumstances. This is particularly true in wave flows past cylinders and Bushnell (ref 34 of the paper) for example demonstrates large increases in the lift force on individual members within an array of cylinders. Lift forces are therefore very important especially in the context of flow interference and should be given due consideration when estimating fluid loading.

N S Miller (Yard)

1. Dr Singh has provided a very wide ranging survey of the problems associated with the calculation of fluid loads and in particular wind loadings.
2. I strongly agree with his conclusion of the Environmental parameters section that 'much could be done to obtain a consistent approach to wind shear over the sea'. He mentions the different recommendations regarding the time averages to be used in deriving the wind velocity for various sizes of structure, but does not indicate the major differences which can exist between Classification Societies. As an example both Lloyd's Register and Det Norske Veritas (DNV) recommend the use of a 1 h average wind speed in mooring calculations, while Bureau Veritas (BV) recommend a 10 min average and, as indicated in the paper, other authorities recommend a 1 min average. These differences can represent up to 40% difference in mooring load.
3. Since the natural frequency of a vessel on its moorings will vary from 1 min to around 10 min, depending on the depth of water and initial pre-tension, it is important that gust effects which have a periodicity in this range are properly taken into account. To this end it appears that the BV recommendations are the most sensible provided due account is taken of the effect of wind gust spectra on the dynamic behaviour.
4. One aspect of the height index which is often not taken into account is that wind tunnel tests are sometimes carried out with profiles which are substantially different from commonly accepted profiles, and the basic velocity used to produce the drag and lift coefficients is not always taken at the scale height of 10 m above the sea. The combination of these factors can lead to substantial errors.
5. The data in Table II in the paper can be somewhat misleading as although the difference in the overall C_D for the semi-submersible appears to be quite small, the differences in the drag for individual elements can be quite large. As an example DNV and ABS use very different drag coefficients for circular columns and the former take interferences into account while the latter do not. Thus depending on the column spacing, very different drags can be obtained. This results in a very different centre of pressure for the two sets of calculations as is shown by the moment calculations in Table II of the paper.
6. The work of Gould (ref 1 below) would suggest that there is some merit in taking wind tunnel tests in a uniform flow over the model depth and then deducting a drag coefficient appropriate to the specific velocity profiles. This would enable the one set of tests to be used both for mooring, where a 10 min average profile might be appropriate, and for stability, where a 1 min average profile is required.
7. The paper does not discuss wind gust spectra which as indicated above are of considerable importance to moored structures. There is no widely agreed formula to predict these spectra and there is a lack of full-scale data at sea which could be used to validate the existing formulae. However they can considerably affect dynamic behaviour at a mooring.

Reference

1. R W F Gould, 'The estimation of wind loads on ship superstructures', Maritime Technology Monograph No 8, RINA (Year).

S Singh (BMT) Dr Miller raises an issue which I only briefly mentioned in the paper, namely the lack of consistency in the guidelines and recommendations provided by the various Classification Societies. Dr Miller points to just one inconsistency in the recommendations from some of the societies but there are a number of other inconsistencies not just in guidelines for fluid loading. With regard to the averaging time for use in mooring calculations, I would support the view that a 1 h average is inappropriate and in the paper I refer to a 1 min averaging period (though this might be on the conservative side).

Turning to the point about wind profiles used in wind tunnel tests, it should be noted that in principle a variety of wind profiles can be simulated in most adequately equipped wind tunnels. There is therefore no reason why wind tunnel tests should not be carried out in an appropriate boundary layer. Furthermore, use of a velocity other than that at the 10 m height to non-dimensionalise the force coefficients should not cause a problem providing that the wind profile is known, since the force coefficients could be converted to be used with the velocity at 10 m.

As stated in my reply to Dr Smith's questions and comments the calculation of local loads (on individual items of a larger structure) must be treated differently from the assessment of total loads. Thus, there can be large differences in the loads on individual items calculated by the different 'building block' methods, such as the ABS and DNV Codes, even though the overall loads are similar. The message then is that these methods are not to be used for assessment of local loads.

I would disagree strongly with any suggestion to perform model tests in uniform low turbulence (smooth) flow. The flow past a number of bluff bodies can be sensitive to the scale and intensity of turbulence which may affect re-attachment lengths etc; mismatch of these fundamental features could then result in an unrealistic flow pattern and hence drag, lift and moment on the structure. For complex structures such as a semi-submersible or a jack-up unit (especially when the former is heeled), the influence of shear can be significant. As I suggest in the paper any doubts regarding the simulation or any aspect of a model test should be investigated by performing appropriate sensitivity studies.

It was my deliberate intention to limit the scope of the paper to mean (steady) forces. However I do accept the importance of the wind spectra when dealing with the dynamic behaviours of the unit. In particular, as mentioned by Dr Frieze, low frequency energy in the wind spectrum can be particularly troublesome to the forces and responses of semi-submersibles.