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THE PREDICTION OF STRUCTURAL RESPONSE TO BUFFET FLOW: A STATE-OF-THE-ART REVIEW

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16. Abstract

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SUMMARY

A brief review of certain aspects of the dynamic system being discussed is presented, and important structural and aerodynamic quantities of the system are discussed. A theoretical model is presented which relates these quantities to each other. These quantities are then each, in turn, considered in terms of the state of the art of determining the quantities and in terms of areas where further research is needed. The similarity laws and scaling relationships applicable to determining buffet structural response are then discussed, and areas where simplification is required or may be permissible are mentioned. Finally, the various types of model tests pertinent to predicting response of the aircraft structure to buffet flow are discussed.

INTRODUCTION

The dynamic system involved in the excitation and response of an airplane to buffet flow is quite complex and although buffet phenomena have received much attention from investigators, the state of the art to definitively predict analytically or experimentally the complete structural response and handling characteristics as the buffet boundary is penetrated leaves much to be desired. (Reference 1 presents a reasonably complete bibliography and summarizes briefly information available on buffet loads on airplanes to about 1959. From the mid-fifties to the mid-sixties much buffet loads research, particularly in the United States, was oriented toward missiles and space launch vehicles configurations. Reference 2 lists many reports pertinent to this work.)

One of the characteristics of this area of research or study is a lack of generally accepted definitions of terms, expressions, and phenomena. There can be, therefore, a sort of "communication gap" among investigators in this field unless care is taken to define the expressions and concepts of the phenomena being discussed. Therefore, a brief review of certain aspects of the "dynamic system" being discussed here will first be presented. The important structural and aerodynamic quantities of the system will then be discussed. A theoretical model will be presented which relates these quantities to each other, and then they will each in turn be considered in terms of the state of the art of determining the quantities, and in terms of areas where further research is needed. The similarity laws and scaling relationships applicable to determining buffet structural response will then be presented and areas where simplification is required or may be permissible will be mentioned. Finally, the various types of model tests pertinent to predicting response of the aircraft structure to buffet flow will be discussed.

PERSPECTIVE OF THE DYNAMIC SYSTEM

The "dynamic system" consists of two parts - a flexible aircraft structure, and an unsteady aerodynamic force field that acts on, or interacts with, the aircraft structure to produce undesired motions, either in terms of dynamic structural deformations, "rigid body" movements, or a combination of both. The unsteady aerodynamic force field may exist without the presence of the aircraft (i.e., atmospheric turbulence) or may be the result of the presence of the aircraft. The latter is the case to be discussed here. These unsteady (generally random) aerodynamic forces are caused by flow separation from the aircraft surface, either due to high incidence or due to shock-boundary-layer interactions; or by turbulent wakes from upstream surfaces or protuberances. The flow that produces these unsteady aerodynamic forces is termed "buffet flow."

The aircraft response to buffet flow (buffeting) may be categorized as local (i.e., panel vibration), structural (whole surface deformations-wing, fuselage, tail surfaces), and "rigid body" (decreased performance, wing drop, wing rocking, "nose slice," "porpoising"). The discussions here will mainly be concerned with structural response to unsteady flow fields that are the result of the presence of the aircraft; that is, response of the aircraft to atmospheric (or tunnel) turbulence is minimal compared to the response to buffet flow. This is not to say that local response is not important. Local panel response is believed to have caused the destruction of the first unmanned Mercury-Atlas launch vehicle; and on airplanes, panel response can be a source of noise discomfort and skin fatigue.

Two areas of buffet flow or buffeting are of interest - the "buffet onset" flight conditions, and the level of intensity of structural loads and rigid body motions as the penetration into the buffet region (in terms of increasing angle of attack or Mach number) continues. In this discussion, attention will be confined primarily to the response of the structure as the buffet boundary is penetrated.

STRUCTURAL AND AERODYNAMIC QUANTITIES OF THE DYNAMIC SYSTEM

Certain fundamental quantities of the dynamic system have to be determined or considered to predict aircraft buffet loads from either theoretical/empirical methods or from scaled model tests. It may be instructive therefore to consider briefly a theoretical model that relates structural response to the random aerodynamic forces of buffeting flow.

Theoretical Model Depicting Important Aerodynamic and Structural Quantities

For illustrative purposes consider a wing (or other surface) flying at constant altitude with constant velocity under flight conditions that are producing buffet flow. The only aerodynamic forces considered present in addition to the random component are damping forces proportional to the velocity of the bending vibrations of the wing. Little loss of generality results from neglecting the aerodynamic inertia and "spring" forces, since such forces usually are small compared with their structural counterparts. Under these conditions a set of differential equations which govern the bending vibration characteristics of the system may be written

$$M_n\ddot{Z}_n(t) + C_n\dot{Z}_n(t) + \omega_n{}^2 M_n Z_n(t) = qS\int_0^1 c_L(\xi,t)h_n(\xi)\,d\xi \quad (n = 1, 2, 3, \ldots) \tag{1}$$

where

ξ	nondimensional spanwise coordinate
$Z_n(t)$	deflection of a point (say the tip) in n^{th} bending mode
t	time
C_n	generalized damping coefficient in n^{th} bending mode, including aerodynamic and structural components
M_n	generalized mass in n^{th} bending mode
ω_n	natural circular frequency in n^{th} bending mode

The right-hand side of Equation (1) is the generalized random aerodynamic load expressed in coefficient form. The function $c_L(\xi,t)$ is the random section lift coefficient and S, q, and $h_n(\xi)$ are, respectively, reference area, free-stream dynamic pressure, and mode shape of the n^{th} bending mode referred to unity at the tip. Using the method of generalized harmonic analysis which was first applied to the analysis of buffeting many years ago (Ref. 3), Equation (1) can be solved approximately for the mean square tip deflection

$$\overline{y(t)^2} = q^2S^2\sum_{n=1}^{\infty}\frac{\pi\,\omega_n}{4M_n{}^2\omega_n{}^4\,(C/C_{cr})_n}\,C_{L,n}(\omega_n) \tag{2}$$

where

$\left(\frac{C}{C_{cr}}\right)_n$	generalized damping coefficient, fraction of critical damping for the n^{th} mode
$C_{L,n}(\omega_n)$	power spectrum of effective random aerodynamic lift coefficient for the n^{th} mode
S	reference area

It has been assumed that the system has small damping and reasonably well separated natural frequencies so that all contributions to the total response are small except in the neighborhood of resonant frequencies. Thus, modal coupling is considered negligible so that the total response can be considered a linear superposition of single-degree-of-freedom responses.

In buffeting studies on elastic structures, usually the acceleration or bending moment at some point on the structure is desired rather than the deflection of the structure. By using Equation (2) and a set of coefficients which relate the acceleration in the n^{th} bending mode at a point to the tip amplitude in that mode an expression for the acceleration may be obtained

$$a^2(\xi_o)_T = S^2q^2\sum_{n=1}^{\infty}\frac{k_n}{M_n{}^2}\,\frac{1}{\left(\frac{C_a}{C_{cr}}+\frac{C_s}{C_{cr}}\right)_n}\,\hat{C}_{L,n}(k_n) \tag{3}$$

where

$a(\xi_o)_T$	total root-mean-square acceleration at a particular location
k_n	reduced frequency for n^{th} natural vibration mode, $k_n = \frac{b\omega_n}{V}$ where b reference length, mean wing chord
V	free-stream velocity

$\left(\frac{C_a}{C_{cr}} + \frac{C_s}{C_{cr}}\right)_n$ sum of aerodynamic and structural damping in n^{th} vibration mode, fraction of critical damping

$\hat{C}_{L,n}(k_n)$ power spectrum of effective random aerodynamic lift coefficient expressed as a function of reduced frequency, $\hat{C}_{L,n}(k_n) = \frac{V}{b} C_{L,n}(\omega_n)$

Thus, the acceleration is dependent on the aerodynamic excitation force in the form of the power spectrum of effective random aerodynamic lift coefficients, the aerodynamic and structural damping, the generalized mass and reduced frequency of each vibration mode of significance. The manner in which these quantities are usually determined will now be discussed.

Unsteady Aerodynamic Excitation Forces

Although there has been some apparent success in calculating the buffet onset conditions for relatively large aspect ratio wings at moderate angles of attack (Ref. 4, for instance), the situation is quite different for the prediction of buffet load intensities in fully developed buffet flow, particularly for fighter aircraft wings with large sweep angles and small aspect ratio operating at high angles of attack and/or high Mach numbers. The problem is primarily the lack of an appropriate aerodynamic theory for calculating the random aerodynamic excitation forces, $C_{L,n}(k_n)$, in the theoretical model just discussed. For this reason investigators have generally turned to wind-tunnel tests for the unsteady pressure distributions needed to provide the buffet aerodynamic excitation forces for the dynamic analysis. The information required to fully describe the random aerodynamic forces are the magnitude and frequency spectrum of the unsteady pressures, their locations and effective area of coverage on the surface, and a pattern of spatial correlation which can be represented by cross-correlation or cross-spectra functions. Although there have been numerous investigations dealing with turbulent boundary layers, separated flow, and shock-boundary-layer interaction (Refs. 5, 6, and 7, for example), the studies have generally been oriented toward measurement of intensities of pressure fluctuations beneath attached turbulent boundary layers at supersonic speeds with very limited analysis of power spectra and spatial correlation data applicable to the separated flow found on a maneuvering high-performance fighter wing. Most such studies in the past have dealt with the flow over space launch vehicles and missiles. Some of the more immediately applicable studies are reported in References 8, 9, 10, and 11. One of the more detailed recent studies of aircraft buffet flow during transonic maneuvers is discussed in Reference 9. Comparisons are given in Reference 9 of root-mean-square pressure coefficients, $\Delta C_{p_{rms}}$ (root-mean-square fluctuating pressure divided by free-stream dynamic pressure), measured on a "rigid" wind-tunnel model and those measured on the full-scale airplane wing at two corresponding locations. Examples of these comparisons are shown in Figure 1. The model pressures were generally somewhat higher than the full-scale pressures. A sample comparison of model and full-scale pressure spectral shape taken from Reference 9 is shown in Figure 2. It was concluded that the model and airplane spectral shapes agreed reasonably well for most of the test conditions. The disparities were apparently greatest at the lower end of the spectrum. Some other conclusions of this study pertinent to the unsteady aerodynamic excitation forces are: (1) at high angles of attack the flow over the wing was quite complex, being influenced strongly by vortices from the leading edge, "snag," and wing tip; (2) disturbances seemed to emanate from multiple sources simultaneously and propagate in a complex manner; (3) the fluctuating pressure spectra frequently exhibited peaks at frequencies believed to be associated with the vortices; and (4) maximum fluctuating pressure coefficients were generally of the order of $\Delta C_{p_{rms}} = 0.2$

Digressing for a moment to the wind-tunnel model/full-scale rms pressure coefficient comparisons, one may speculate on several reasons for the differences. There is, of course, always the nagging doubt about Reynolds number effects, tunnel turbulence, and wall effects. Then, there is the difference in the manner in which the variation of unsteady pressure with angle of attack is achieved. (The mean angle of attack in the tunnel is essentially steady, whereas in flight, particularly at the higher speeds, the angle of attack is continuously changing.) A more fundamental question is whether the unsteady aerodynamic excitation forces on a rigid (nonmoving) wing are the same as those on an identical but flexible (responding) wing. That is, does the tendency of the flexible wing to move with the driving force tend to reduce those forces relative to the forces acting on a nonmoving surface? Some definitive experiments are needed to answer this question. J. G. Jones, in Reference 12, discussed in some detail the interpretation of fluctuating pressures associated with separated flow measured on nonmoving and responding wings for evaluation of the unsteady aerodynamic excitation forces, and in Reference 13, L. E. Ericsson presents a semiempirical analysis that uses static experimental data as an input to attempt to explain some of the dynamic effects of shock-induced flow separation.

Aerodynamic and Structural Damping

Another factor needed to predict quantitatively the response of the structure to buffet flow is the total system damping which can be broken down into two components - aerodynamic and structural, neither of which can be readily determined explicitly at buffet flight conditions. The aerodynamic damping is itself a function of flight condition (density and velocity), mean angle of attack, and oscillation frequency (Ref. 14). A simplified relationship between these parameters and the aerodynamic damping ratio $\frac{C_a}{C_{cr}}$ developed in Reference 14 for a wing oscillating in the fundamental bending mode is

$$\frac{C_a}{C_{cr}} = \frac{\Pi \rho V F_e \cos \bar{\alpha} \int_0^L b(y)[h_1(y)]^2 \, dy}{2\omega_1 M_e} \tag{4}$$

where ρ free-stream density

V free-stream velocity

$\bar{\alpha}$ mean angle of attack about which oscillation occurs

ω_1 first natural frequency

M_e equivalent mass

$b(y)$ local chord

$h_1(y)$ first bending mode shape

L wing span

F_e effective value of aerodynamic damping coefficient

It has been common practice in scaling buffet loads from one flight condition to another to assume that, other factors being equal, the aerodynamic damping ratio is proportional to (ρV) and sufficiently greater than the structural damping so that the structural damping is negligible (Refs. 15 and 16, for example). Under these assumptions it may be seen from Equation (3) that for a particular mode the root-mean-square acceleration response is proportional to the square root of the dynamic pressure. If, on the other hand, it is assumed that the only significant damping is structural, then the rms response is directly proportional to the dynamic pressure. The true case, of course, is somewhere between these two extremes. The relative magnitudes of aerodynamic and structural damping are subject to some contradiction in the literature. There is some experimental evidence that at least for long slender bodies, such as launch vehicles, the rms buffet response is inversely proportional to the square root of total damping as indicated in Equation (3). For example, Figure 3 is based on results from Reference 17 where the total damping of a launch vehicle aeroelastic buffet model was varied electromagnetically under wind-on conditions. The relative changes in rms buffet bending moment, σ_Δ/σ, with relative changes of total damping

$$\left[\frac{\frac{C_a}{C_{cr}} + \frac{C_s}{C_{cr}} + \frac{\Delta C_s}{C_{cr}}}{\frac{C_a}{C_{cr}} + \frac{C_s}{C_{cr}}}\right]_n^{1/2}$$

for the first bending mode of one configuration and the first three bending modes of a second configuration are shown to follow reasonably well the curve defined by

$$\left[\frac{1}{\frac{C_a}{C_{cr}} + \frac{C_s}{C_{cr}}}\right]^{1/2}$$

Figure 4 from Reference 15 showing the variation of rms wing root bending moment with density at constant Mach number (measured on an F-86A fighter) indicates the response to be proportional to $(\rho)^{1/2}$ so that the damping must be primarily aerodynamic in nature. Figure 5 from Reference 16 which compares rms bending moments measured on geometrically identical model wings made from aluminum and from magnesium with calculated values scaled from bending moments measured on a geometrically identical but much stiffer wing made from steel. The experiment was designed so that some of the many parameters which are of importance in buffeting such as Mach number, Reynolds number, and reduced frequency were held essentially constant so that other factors, such as effects of damping on scaling relationships, could be evaluated. The data are presented for three different scaling relationships used to scale the data from the steel model: in Figure 5(a) both aerodyanmic damping (calculated after Ref. 14) and structural damping (measured under no-wind conditions) were used in the scaling relationship; in Figure 5(b) aerodynamic damping only was used; and in Figure 5(c) only structural damping was used. It was concluded that the prediction based on aerodynamic damping only, which apparently contained compensating inaccuracies, provided values of buffeting loads which were closer to the measured values than those predicted by the more complete analysis including both structural and aerodynamic damping. There is other evidence (Refs. 18, 19, and 20, for example) to suggest that wing damping of solid metal wind-tunnel models is predominantly structural. It is apparent that the character of system damping in buffet flow needs further study.

Attention will now be turned to some of the more common means of determining damping. Analytical methods of determining aerodynamic damping are almost exclusively confined to empirical means based on measurements or based on aerodynamic theories applicable to attached subsonic or supersonic flow that bears little relation to flow experienced beyond the buffet boundary. Structural damping is usually measured under "no-wind" conditions by various methods, as is total damping under flight or wind-on conditions. The aerodynamic damping is taken to be the difference between "wind-on" and "wind-off" damping. It is worth mentioning that both structural and aerodynamic damping may very well be amplitude dependent so that in deducing aerodynamic damping from total damping measurements care should be taken that wind-on and wind-off measurements are made at the same amplitude or that the variation with amplitude is established.

Various methods of determining damping are presented in the literature (Refs. 14, 21-24, for example). Reference 22 contains a general review of experimental techniques that are discussed in some detail. A complicating factor in the measurement of damping under separated flow conditions, however, is the random response of the model, that is, the damping generally must be measured as statistical means averaged over many cycles. A recently devised technique (R 4) known as "random-dec" appears to be particularly attractive for determining total damping under either full-scale or wind-tunnel model buffet flow conditions. Basically, as indicated schematically in Figure 6(a), the method extracts the damped sinusoidal response of the structural vibration modes from the total structural response to either an externally applied force or, more importantly, to the random buffet excitation forces. By averaging the measured response over a number of time-sweeps that are started at a given response amplitude, the response of the system to a "step input" is determined. (The measured response of the system can be considered to be composed of the response to a step, an impulse, and a random force. The response to an impulse and to a random force average to zero.) Damping is obtained in the same way as from a free vibration decay since the decrement or "random-dec signature" is representative of the free vibration decay curve which would be obtained if the structure were displaced to the selected amplitude and suddenly let go. For single-degree-of-freedom linear systems excited by white noise, the random-dec signature is identical in form to the autocorrelation function, but for multi-degree-of-freedom systems and nonlinear systems, it differs in that troublesome cross-products which occur in the autocorrelation of closely coupled modes are absent. Real systems, of course, contain many modes and several techniques can be used to reduce the response to an effective single-degree-of-freedom system for each mode of interest. Figure 6(b) shows an example of the random-dec technique applied to the acceleration response of a model wing to tunnel turbulence. The actual response time history is shown in the upper part of the figure. The difficulty of determining damping from this kind of response is obvious. In the lower part of the figure the random-dec signature for this response is shown from which the damping can readily be determined from the well-known equation

$$\tilde{g} = \frac{1}{\pi N} \ell n \frac{X_0}{X_N}$$

It is concluded in Reference 24 that for systems with modes closely spaced in frequency, application of the technique is not so straightforward and that further work is needed to define the limitations and precision of measurements for such cases.

Generalized Mass

The final modal factor to be considered for the determination of buffet response from application of dynamic scaling relationships such as Equation (3) is the generalized mass. (The reduced frequency, k_n, is simply a function of modal frequency.) Mass effects appear in the structural analysis of dynamic systems in the form (for a planar system)

$$M_n = \iint m(x,y) h_n^2 (x,y)\, dx\, dy \tag{5}$$

where M_n generalized mass associated with mode n

x,y physical coordinates

$h_n(x,y)$ normalized deflection at x,y

$m\,(x,y)$ mass per unit area at x,y

The integral can be evaluated for each mode by using either a known mass distribution and mode shape which have been determined experimentally or analytically; or the generalized mass can be determined directly by experimental means if the structure exists. A widely used method (Ref. 25) considers the change in frequency caused by the addition of a small mass. Briefly, if the generalized stiffness of the n^{th} vibration mode is defined as

$$K_n = M_n \omega_n^2$$

where ω_n is the natural frequency of the n^{th} structural mode, and if it is assumed that the addition of a small known mass, ΔM does not change the generalized stiffness, then

$$K_n = (M_n + \Delta M\, h_{n,\Delta}^2)\, \omega_{n,\Delta}^2$$

where $h_{n,\Delta}$ ratio of modal deflection at point where incremental mass is added to deflection at station for which generalized mass is desired

$\omega_{n,\Delta}$ natural frequency of n^{th} mode with added mass ΔM

therefore

$$(M_n + \Delta M\, h_{n,\Delta}^2)\, \omega_{n,\Delta}^2 = M_n \omega_n^2$$

or

$$M_n = \frac{\Delta M\, h_{n,\Delta}^2}{\left(\frac{\omega_n}{\omega_{n,\Delta}}\right)^2 - 1}$$

For each mode, ΔM is known and $h_{n,\Delta}$, ω_n and $\omega_{n,\Delta}$ are measured. In practice, it is convenient to plot $\Delta M\, h_{n,\Delta}^2$ as a function of

$$\left(\frac{\omega_n}{\omega_{n,\Delta}}\right)^2 - 1$$

for various values of ΔM. The slope of this curve, evaluated over the linear portion near zero, is the generalized mass. Difficulty may be experienced in applying the method if the modes are not well separated or if the damping is large.

Recent advances in "system identification" techniques (the process of determining parameters in the equations of motion of a system directly from test data) made possible, in part, by increased computer capability and accuracy, hold promise of providing better methods of determining generalized mass as well as system stiffness or damping. A good review of the state of the art of system identification techniques is presented in Reference 26.

SIMILARITY LAWS AND SCALING RELATIONSHIPS

The similarity laws and scaling relationships for predicting from wind-tunnel model tests the response of the full-scale airplane structure (in terms of root-mean-square accelerations, bending moments, etc.) to buffet flow conditions will now be discussed. The similarity requirements will first be considered on the basis of what is necessary to predict quantitatively the response of specific aircraft configurations as contrasted to requirements for "trend studies." Then suitable relaxations of these requirements dictated by practical considerations and a more liberal interpretation of scaling relationships to meet less stringent objectives will be discussed.

Relative Importance of Similarity Laws

In principle, a model that meets the similarity requirements for flutter testing will also be suitable for direct scaling of buffet response to full-scale values. (However, there are considerations, to be discussed later, in addition to similarity requirements that make the design and construction of a dynamically scaled aeroelastic buffet model more difficult than a similar flutter model.) Discussions of the basic requirements for achieving dynamic similarity of model and full-scale aircraft abound in the literature (Refs. 27-30, for example) and will only be reviewed here briefly as they apply to transonic buffet loads studies. The similarity requirements are generally deduced by applying the Buckingham II theorem of dimensional analysis or by examining the appropriate governing equations in nondimensional form. For a flexible body completely immersed in a fluid with relative motion between the body and the fluid these procedures result in independent nondimensional parameters which may be thought of as ratios of the potentially significant inertia, viscous, elastic, and gravity forces that act on the body and fluid. The more important ones to be considered are:

(1)	$\frac{V}{c}$	Mach number, M
(2)	$\frac{b\omega}{V}$	reduced frequency, k
(3)	$\frac{m}{\rho b^2}$	mass density ratio
(4)	$\frac{\rho V b}{\mu}$	Reynolds number
(5)	$\frac{V^2}{bg}$	Froude number

where	a	fluid free-stream speed of sound
	V	fluid free-stream velocity
	ρ	fluid free-stream density
	μ	fluid free-stream dynamic viscosity
	g	acceleration due to gravity
	b	characteristic length
	ω	characteristic oscillation frequency
	m	body mass per unit length

These five basic independent dimensionless parameters result from several assumptions regarding characteristics of the body and the fluid, that is: (1) the fluid is compressible and behaves as a perfect gas but the velocity range is low enough so that effects of kinetic heating are insignificant; and (2) the body is completely immersed so that surface tension effects may be ignored. Implicit in the five basic parameters is another, the ratio of the specific heat of the fluid, γ, and if dissipative forces are considered a further parameter, the ratio of structural damping to critical damping, C_S/C_{CR}, may be added. From these basic similarity parameters other dependent ratios relating model quantities to full-scale quantities may be derived. If these dimensionless parameters have the same values for the model and the full-scale aircraft and the mass, stiffness, and to a lesser degree the damping distributions are the same for the model and full-scale aircraft, then the flexible and rigid body response or behavior of the model will be similar to the aircraft providing the model is geometrically similar to the aircraft, orientation to the airflow is similar to that of the aircraft, and the model is supported in a manner that does not significantly affect the model response or behavior.

The simultaneous satisfaction of all the similarity parameters in a single model or test is not practical. The degree to which the various parameters may be ignored or approximated is a function of the test objective and the available tunnel performance. For example, if the purpose of the test is to determine buffet response at high angles of attack at relatively low speeds the Mach number need not be the same, whereas, for response at high speeds the model and airplane Mach numbers should definitely be simulated. The gravitational parameter is not usually simulated except for studies where static deflections or aeroelastic deformations are important. For buffet model response studies with the objective of predicting full-scale buffet loads quantitatively, the mass density and reduced frequency similarity parameters are as important as they are in flutter proof tests, and much effort is made to have these parameters the same for model and full-scale aircraft. At best, however, the parameters are strictly satisfied in a single model only for the tunnel/flight conditions chosen as the design point. It is generally assumed that the slight deviations from these parameters caused by testing at conditions not far removed from the design point do not significantly affect flow-response similarity and that the resulting model measurements at a particular velocity (or Mach number for high-speed tests) may be scaled to other altitudes or densities by application of suitable scaling relationships. Full-scale values of Reynolds number are quite difficult to achieve because of wind-tunnel performance limitations and the conflicting requirements of other similarity parameters. Viscous flow phenomena, including boundary-layer type, thickness, and separation conditions are influenced in varying degrees by the value of the Reynolds number, and so this parameter would appear to be somewhat more significant for buffet studies than for flutter tests. Although the locations of local shocks and commencement of local separated flow may be Reynolds number dependent in varying degrees depending on the particular aerodynamic configuration, there is some experimental evidence to suggest that the integrated effects on the structural response and even on total lift may be small. For example, in Reference 10 which compares buffet pressures measured in flight with wind-tunnel data (Reynolds number range approximately 3-20 million), it is concluded that for a sharp unswept wing at Mach numbers to 0.7 Reynolds number effects were small. Figures 7 and 8 taken from Reference 31 provide some further evidence from wind-tunnel/flight comparisons. The data are from measurements made on a complete dynamically scaled aeroelastic buffet model being flown on a cable mount system at model Reynolds numbers of 0.87 to 1.33 million compared to flight values of 20 to 28 million. (The Reynolds numbers mentioned on page 10 of Ref. 31 are, in fact, Reynolds number per foot rather than absolute values as stated.) Figure 7 compares the model and full-scale variation of normal force coefficient, C_N, with angle of attack well beyond the buffet boundary. The model and airplane values are seen to agree reasonably well except for the 72° sweep case. Figure 8 compares model and airplane buffet onset (in terms of normal-force coefficient) and buffet boundary penetration as a function of Mach number. Again, the model predicted buffet boundary agrees well with flight values except for the high sweep condition. Thus, although the locations of local shocks and commencement of separated flow may have been different for the airplane and model, their integrated effects on the structural response apparently were small, at least for the lower sweep cases. Reynolds number effects on aerodynamic simulation are discussed extensively in Reference 32. Just how much the Reynolds number requirements may be relaxed for buffet flow conditions has not been conclusively established in the literature, and further studies are needed. For the present, because of wind-tunnel performance limitations, practical model fabrication considerations, and the overriding importance of testing the model at a mass-density ratio comparable to that of the airplane, no attempt normally is made to simulate Reynolds number in high-speed flutter models and the same compromise has to be made for high-speed buffet response models. From similar considerations the gravitational parameter (Froude number) is usually ignored.

The total damping is certainly an important parameter in structural response buffet tests. Unfortunately, modeling technology has not advanced to the point where it is possible to simulate full-scale structural damping (even assuming it is known) so attempts are usually limited to keeping the model structural damping to reasonably small fractions of critical damping. The rationale is that the structural damping is a relatively small part of the total damping and therefore its significance is lessened. This assumption is not always valid, of course. For example, the buffet boundary may be quite close to the flutter boundary in which case the aerodynamic damping might be very small indeed.

The ratio of specific heats similarity parameter is generally automatically satisfied when the model tests are conducted in air. (The satisfaction of this parameter provides similarity in the compressible behavior of the gas under adiabatic conditions and is necessary for exact similarity of the flow pattern.) However, tests in gases heavier than air offer several advantages including easier model construction, higher Reynolds numbers, and lower tunnel power requirements (see Refs. 33 and 34, for example), and there is evidence that it is not always necessary to satisfy this condition for Mach numbers less than about 1.4. (Freon-12 is routinely used in the NASA Langley Research Center Transonic Dynamics Tunnel for flutter and buffet response tests.) However, for tests where local shock location is critically important the parameter can be significant.

Scaling Relationships for Structural Response

Once a geometrically similar model has been constructed so that the stiffness and mass distributions are similar to the full-scale aircraft, and the values of stiffness and mass are reasonably close to the values dictated by the similarity parameters, for the model measurements to be useful to predict full-scale structural loads the necessary buffet response scaling relationships must be known. The theoretical buffet model which relates the various important structural and aerodynamic quantities (Eq. (3) for example) may serve as a basis for developing a scaling relationship for root-mean-square buffet acceleration. Taking the subscript r to indicate airplane-to-model ratio, and subscripts M and A to refer to model and airplane quantities, respectively, Equation (3) may be written

$$a^2(\zeta_o)_{T,A} = b_r^4\, q_r^2 \frac{1}{m_r^2} \sum_{n=1}^{\infty} k_{n,r} \left[\frac{\left(\frac{C_a}{C_{cr}} + \frac{C_s}{C_{cr}}\right)_{n,M}}{\left(\frac{C_a}{C_{cr}} + \frac{C_s}{C_{cr}}\right)_{n,A}}\right] [\hat{C}_{L,n}\,(k_n)]_r\, a^2(\zeta_o)_{n,M} \tag{6}$$

where the generalized mass, M_n, has been replaced by the physical mass ratio, m_r (since the full-scale and model mode shapes are optimally identical).

For a dynamically scaled aeroelastic buffet model that meets the similarity requirements so that it responds in vibration modes similar to those of the aircraft the terms $k_{n,r}$ and $[C_{L,n}(k_n)]_r$ optimally will be unity when the model is tested at design point flow conditions. Because of variations of the speed of sound in the wind tunnel with respect to the speed of sound in flight the actual reduced frequency, $k_{n,r}$ will vary slightly from unity, therefore, the term will be retained for increased scaling accuracy since it is readily measurable. Although the power spectrum of effective random aerodynamic lift coefficient ratio, $[C_{L,n}(k_n)]_r$, is a function of reduced frequency, for small variations, it is assumed to remain unity. At the scaling design point flow conditions, the damping term in brackets in Equation (6) will normally be very near unity for dynamically scaled aeroelastic models with relatively low structural damping. However, this is not true for model test conditions removed from the design point, or for tests in flow conditions where the aerodynamic damping is relatively small, unless the model and airplane structural damping are identical. The value of the damping term may be evaluated by measuring the model aerodynamic damping and structural damping, calculating the airplane aerodynamic damping from the relationship

$$\left(\frac{C_a}{C_{cr}}\right)_{n,A} = \left(\frac{\rho\ V\ b^2}{m\ \omega_n}\right)_r \left(\frac{C_a}{C_{cr}}\right)_{n,M} \quad \text{(See Ref. 14, for example.)}$$

and estimating or measuring the airplane structural damping. The reduced frequency ratio, $k_{n,r} = \frac{b_r\ \omega_r}{V_r}$ is generally taken to be the same for all modes (although in practice there is usually some variation in frequency ratio, ω_r, for the various modes). Equation (6) may therefore be written

$$a^2(\xi_o)_{T,A} = b_r^4\ q_r^2\ k_r\ \frac{1}{m_r^2} \sum_{n=1}^{\infty} \frac{1}{(C_T/C_{cr})_{r,n}}\ a^2(\xi_o)_{n,M} \tag{7}$$

where

$$\left(\frac{C_T}{C_{cr}}\right)_{r,n} = \frac{\left(\frac{C_a}{C_{cr}} + \frac{C_s}{C_{cr}}\right)_{n,A}}{\left(\frac{C_a}{C_{cr}} + \frac{C_s}{C_{cr}}\right)_{n,M}}$$

An expression similar to Equation (7) can be derived for the airplane root-mean-square buffet bending moment at a particular location

$$\sigma^2(\xi_o)_{T,A} = b_r^6\ q_r^2\ k_r \sum_{n=1}^{\infty} \frac{1}{(C_T/C_{cr})_{r,n}}\ \sigma^2(\xi_o)_{n,M} \tag{8}$$

Actually it is not generally possible to measure directly the bending moment $\sigma(\xi_o)_{n,M}$ or acceleration $a(\xi_o)_{n,M}$ buffet response of the model in a particular mode. Rather, the total response at a particular location $\sigma(\xi_o)_M$ is measured and the "modal composition" of the indicated response is estimated from power spectra. Several conditions are inferred in this application of the scaling relationships: (1) the "natural vibration modes" of importance in the total response can be identified, are well separated, and are lowly damped; (2) the total measured model response can be treated as a summation of individual modal responses,

that is
$$\sigma^2(\xi_o)_M = \sum_{n=1}^{\infty} \sigma^2(\xi_o)_{n,M}$$

and (3) the structural and aerodynamic damping of the modes are known or can be estimated. Further, it should be noted that Equations (7) and (8) relate measurements made on the model at a particular location to full-scale values at the same location. If full-scale maximum response values are desired (as is usually the case) and for some reason the measurements are made on the model at a location different from that where the maximum bending moments or accelerations occur, or if a distribution of the loads is desired from a single location measurement, then a measurement location "sensitivity factor" must be determined for each contributing vibration mode. This may be accomplished in principle by calculating the bending moment and/or acceleration distributions due to inertia loading per unit deflection for motion in each mode.

MODEL TESTING

Rigorous application of buffet scaling relationships such as Equations (7) and (8) is seldom possible so that certain simplifying assumptions are usually required, depending on the type and purpose of the test. There are basically two different methods of quantitatively predicting aircraft buffet response: (1) measure buffet accelerations or strains on a dynamically scaled aeroelastic model (Refs. 31 and 35, for example) and (2) measure the pressure fluctuations on a nominally rigid model and then calculate the dynamic response when these pressures act on the flexible structure (Ref. 8, for example). Each method has its advantages and disadvantages. The practical application of these and other relationships will now be discussed for several types of buffet studies.

Dynamically Scaled Aeroelastic Models

The most direct approach for predicting full-scale buffet response loads with a minimum of scaling assumptions is the measurement of the buffet responses of a reduced size dynamically scaled aeroelastic model supported in wind-tunnel flow that accurately simulates the airflow over the airplane in a manner such that the model and airplane degrees of freedom and inertia loads are properly related. In theory, buffet response predictions based on this approach should be the most accurate of the several methods to be discussed. In practice, it is impossible to meet all the above requirements and certain assumptions still are required concerning the significance of the effects of parameters over which little co[illegible] can be exercised. For instance, pitch-rate effects are usually assumed to be negligible for rat[illegible]le to a maneuvering aircraft. Also, it is usually necessary to assume that wind-on mode shape[illegible] esse[illegible]lly the same as wind-off shapes, that there are no significant differences in model and airpla[illegible]ode shap[illegible] that are important in the buffet response, and that the model and airplane structural damp[illegible] in a parti[illegible]lar mode is independent of vibration amplitude, temperature, and flow conditions. A major disadvantage of the approach is the complex and costly modeling requirements, but some conditions or circumstances make the dynamically scaled aeroelastic model test the desirable approach. For instance, when a complete flutter model is required or is desirable for flutter proof tests to minimize expensive flight flutter testing, the extra expense required to make the model suitable for buffet loads testing (primarily due to strength and instrumentation considerations) may be acceptable. The approach may also be desirable when components other than the wing are considered to be buffet critical. When a maneuvering aircraft "penetrates the buffet boundary" each part of the aircraft experiences its own boundary. Although attention is usually focused on the wing response as a function of increasing angle of attack (say), in fact, an all-movable horizontal tail, on sudden deflection for an abrupt pullup maneuver, may be the first component to experience buffeting conditions. In fact, there have been several instances where the design loads on tail surfaces have been exceeded due to buffeting. Although the horizontal tail is normally considered to be the critical tail component, during recent wind-tunnel buffet studies on a fighter airplane it was found that the critical components at high angles of attack were the vertical tails which were vibrating primarily in a torsion mode. Of course, when buffeting flow is encountered anywhere on the aircraft the entire structure responds with the load or acceleration intensities varying over the aircraft according to its structural characteristics. The highly maneuverable high-performance fighter, typically flown well into the buffet boundary, and subject to buffet flow due to shock-boundary-layer interaction, high angle of attack, and wake impingement presents a formidable challenge to predict the response characteristics. It has generally been found that even for these conditions, wing buffet loads are not the critical consideration but rather (1) vibrations which subject fire control, navigation and reconnaissance equipment, instruments, and crew to a more severe operational environment and increase fatigue; (2) degradation of performance through increased drag and decreased lateral stability which detracts from tracking capability; and (3) as mentioned previously, excessive structural loads on tail and control surfaces.

When complete aircraft buffet response acceleration and load predictions are required, the dynamically scaled aeroelastic model test would seem to offer the best hope of obtaining suitable data. This technique has been evaluated in Reference 31 by comparing the scaled buffet bending moments and accelerations measured on a 1/8-scale flutter model of a variable-sweep fighter airplane with those measured in a flight buffet research program (Ref. 36). The model was "flown" on a cable mount system with a lift balancing device which counteracted the lift in excess of the model weight, thus allowing the model to be flown under conditions simulating high load factors (neglecting inertia and rate effects, of course). Figure 9 is a schematic representation of the system which was designed to provide a relatively constant low-level spring rate so that the model could respond in rigid and elastic body dynamic motions with a minimum of restraint

Figure 10(a) compares the airplane buffet response with model-predicted values (using scaling relationships shown in Eqs. (7) and (8)) of wing and horizontal tail rms bending moments and rms accelerations at the center of gravity. The data are typical in that the full-scale buffet bending moments on the wing and horizontal tails and the center-of-gravity buffet accelerations predicted from the model data agreed well with airplane values at all Mach numbers at a wing sweep angle of 26°. At wing sweep angle of 50° the agreement was reasonably good at all Mach numbers tested for the wing bending moments, but the correlation of the model and airplane center-of-gravity accelerations and horizontal tail bending moments was not so good at the higher Mach numbers. At 72° sweep, Figure 10(b), both the airplane and model data exhibited a large degree of randomness at extremely low levels of buffet response which made evaluation of the technique difficult.

The right side of Figure 11 shows sample comparisons of the model and airplane horizontal tail bending-moment response spectra. Here the model response is primarily in the horizontal-tail-first symmetrical bending mode with secondary response in the fuselage vertical bending mode. Unpublished airplane spectra indicate that the primary response varied between horizontal-tail bending and fuselage vertical bending depending on flight condition and whether the right or left tail was being considered. On the left side of Figure 11 the wing-root bending-moment response spectra of the model and airplane are compared. The spectra are almost identical with the response of both being primarily in the first symmetrical bending mode. It is this characteristic of wing-root bending-moment buffet response that makes possible the use of much simplified models (to be discussed next) under certain conditions to estimate full-scale wing response. Except for the wing, however, power spectral density analyses of the model response to buffet flow showed that the indicated modal composition of the total measured response was dependent on the type and location of the measurement, wing sweep angle, Mach number, and, in some cases, the depth of penetration into the buffet region. These conclusions from the model studies discussed here have been borne out by more recent flight-test data from a more extensively instrumented aircraft (Ref. 37). These flight studies showed, however, that although the wing-root bending-moment response spectrum was predominantly in the wing-first bending mode, bending moment and acceleration response measured further outboard on the wing, particularly near the tip, consisted primarily of wing-second antisymmetric bending and higher modes.

Stiff But Responding Models

In the dynamically scaled aeroelastic model approach to predicting absolute values of full-scale buffet loads, the reduced stiffness and mass density similarity ratios, and stiffness and mass distributions are satisfied by design so that, assuming an adequate model, the modal response of the model is identical to that of the full-scale airplane. Therefore, all the quantities needed in Equation (6) to scale model response loads to full-scale values are either identically unity or can be explicitly measured or calculated except for full-scale structural damping. Variants of this approach may be used to gain insight into the buffet phenomena, to investigate the validity of certain assumptions made in theoretical modeling, and to estimate relative intensity levels and boundary penetration characteristics of different geometrical configurations. For instance, considerable information may be deduced about wing buffet onset conditions and the relative rise in response intensity with buffet boundary penetration by measuring the wing bending-moment response of conventional "semirigid" wind-tunnel models (Refs. 15, 38, 39). This method may be valid when the conventional wind-tunnel model wing-first bending mode is similar in shape and scaled frequency to that of the full-scale aircraft and the model structural damping is reasonably low. One must also assume the response of the wing is in the first bending mode only, and that response of other components of the structure are isolated and do not influence the response of the component being evaluated. The use of this type of model is generally restricted to wing buffet studies - primarily for buffet onset, but it is used in some instances for load intensities as the buffet boundary is penetrated or the aerodynamic configuration changed. Buffet onset predictions have generally been good using this technique. The prediction of absolute buffet loads on wings has met with varying degrees of success likely depending on the relative magnitudes of model structural and aerodynamic damping, mass density ratio, and tunnel turbulence levels. The method has been used since the early fifties but wind-tunnel/flight correlation buffet loads data are sparse. Figures 12(a) and 12(b) from Reference 15 and Figure 12(c) from Reference 40 are indicative of results achieved using this technique.

A refinement of this approach that makes use of "stiff" conventional models to predict maximum flight penetration buffet boundaries is suggested in Reference 39. The basic hypothesis is that the tunnel turbulence or unsteadiness (which must be known in terms of unsteady pressure or flow angle power spectra) can be used as a given level of aerodynamic excitation to calibrate the model response at the wing fundamental frequency, and hence to derive buffeting coefficients from buffet strain measurements on the wing. Figure 13 from Reference 41 illustrates a recent test of this hypothesis. The method is discussed quite fully in References 39 and 41.

"Rigid" Buffet Pressure Models

Both the dynamically scaled aeroelastic model approach and tests on "stiff" but responding models make use of measurements of model response to predict full-scale buffet characteristics. A completely different approach makes use of measurements of buffet fluctuating pressures on "rigid" models to predict analytically the full-scale buffet loads on the flexible aircraft. The term "rigid" is meant to imply that no significant model response mode occurs at frequencies in the vicinity of the scaled frequency at which buffeting intensity is to be predicted on the full-scale aircraft. The basic method, briefly outlined here, is discussed in some detail in the literature (Refs. 8 and 42, for example). Basically, the method entails (1) the measurement of the unsteady pressure distribution on the rigid model in terms of root-mean-square pressure levels on prescribed areas, power and cross-power spectral density functions and corre[illegible] functions, (2) definition of the dynamic characteristics of the aircraft analytically or by vibr[illegible]sts, (3) combining pressure and modal displacement data to yield the buffet forcing function, a[illegible]plying the forcing function to the elastic system to compute the required buffet response loads a[illegible]acements. The practical application of the method requires many assumptions at each stage, particula[illegible]the generation of the buffet forcing function, and requires that an estimate of the total modal damping be available. This approach has been used (Ref. 8) to calculate buffeting at low speeds of a slender wing with a leading-edge vortex. A modification to this approach has been used in Reference 42 to calculate the buffet response of a swept-wing fighter at transonic speeds. A 1/10-scale conventional wind-tunnel model was used in this series of tests designed to learn as much about the buffet flow field as possible. Here the "rigid" model did have scaled frequencies near those of the full-scale airplane which implies an assumption that any wing motion effects on the buffet flow were the same for model and full-scale airplane. Figures 14(a) and 14(b) from Reference 42 compare predicted and measured acceleration response at Mach numbers of 0.79 and 0.92, respectively. Predicted spectra are shown for two different modal damping distributions. One distribution was obtained from the sum of the structural and aerodynamic damping (obtained from velocity versus damping flutter solutions) in each of 20 wing modes used in the analysis. The other was obtained from a constant damping ratio of 0.05 used for all 20 modes. The manner in which the damping was considered is seen to have little effect on the spectra compared to the differences between the measured and predicted responses. The authors conclude that the agreement is reasonable and note that the specific method employed is considered a "first-generation" approach to which refinements in technique may be added.

CONCLUDING REMARKS

The important structural and aerodynamic quantities associated with a flexible aircraft responding to buffet flow conditions, requirements for simulating or calculating the response with the aid of wind-tunnel models, required scaling relationships, and test methods have been discussed relative to the state of the art and to considerations, assumptions, and idealizations that are usually required. Some examples of contradictory evidence in the literature regarding some of these assumptions and idealizations have been cited to indicate areas where further research is required, and some recent results obtained by investigators using several prediction techniques have been compared with measured flight buffet loads.

A significant advancement in buffet analysis techniques was made in the early fifties with the application of the methods of generalized harmonic analysis, and there has since been notable contributions to the understanding of buffet phenomena. However, the state of the art to definitively predict analytically or experimentally the complete structural response and handling characteristics as the buffet boundary is penetrated leaves much to be desired. Although some progress has been made in theoretical methods for predicting buffet onset conditions, particularly at subsonic speeds, no adequate theoretical method exists for calculating the unsteady aerodynamic excitation forces in fully developed separated flow at transonic speeds. The determination of these excitation forces from wind-tunnel model tests for subsequent use in structural response calculations requires many assumptions that need to be validated. A fundamental question that needs to be resolved is whether the unsteady aerodynamic excitation forces on a rigid (nonmoving) wing are the same as those on an identical but flexible (responding) wing, or at least the conditions under which they may be considered the same. The practical significance of Reynolds number effects on separated flow in terms of the effects on structural and rigid body response need to be determined. The effect of rate of change of angle of attack on buffet load intensity needs further study. A systematic study of aerodynamic damping variation with approach to, and penetration of, buffet boundaries is needed along with better means of measuring damping under these conditions.

In spite of these uncertainties, several prediction techniques have been shown to give results that compare favorably with flight data for certain categories of buffeting, most notably wing buffet onset and, to some extent, buffet load intensity. However, experience has shown that even well beyond the buffet boundary wing buffet loads are not usually the critical consideration but rather excessive loads on tail surfaces, vibrations which subject fire control, navigation equipment, instruments, and crew to a more severe operational environment and increase fatigue problems, and degradation of performance through increased drag and decreased lateral stability which detracts from tracking capability.

None of the various approaches to the prediction of buffet response accelerations and loads is completely satisfactory, each having its own advantages and disadvantages. A complicating factor for all methods is the extreme configuration dependency of the various buffeting excitation forces that occur on the aircraft.

Perhaps an additional comment is pertinent regarding the uncertainty of model/flight comparisons. The fact that the buffeting essentially is a random process leads to difficulty in comparing flight buffet loads from a maneuvering aircraft to those predicted from wind-tunnel tests. For example, high-load-factor flight data sample lengths of buffet loads as a function of angle of attack are usually too short to provide the stationarity of the data required in scaling relationships. In addition, the aircraft is not usually maneuvered in such a way that Mach number and density remain constant (as in the usual wind-tunnel test) while the angle of attack is increased to values well beyond the buffet boundary. Until the important parameters can be separated and better controlled, the possibility must be recognized that the degree of correlation between predicted and measured flight buffet response characteristics may be fortuitous.

The prediction of maneuvering aircraft buffet structural response is obviously a fertile field for imaginative research.

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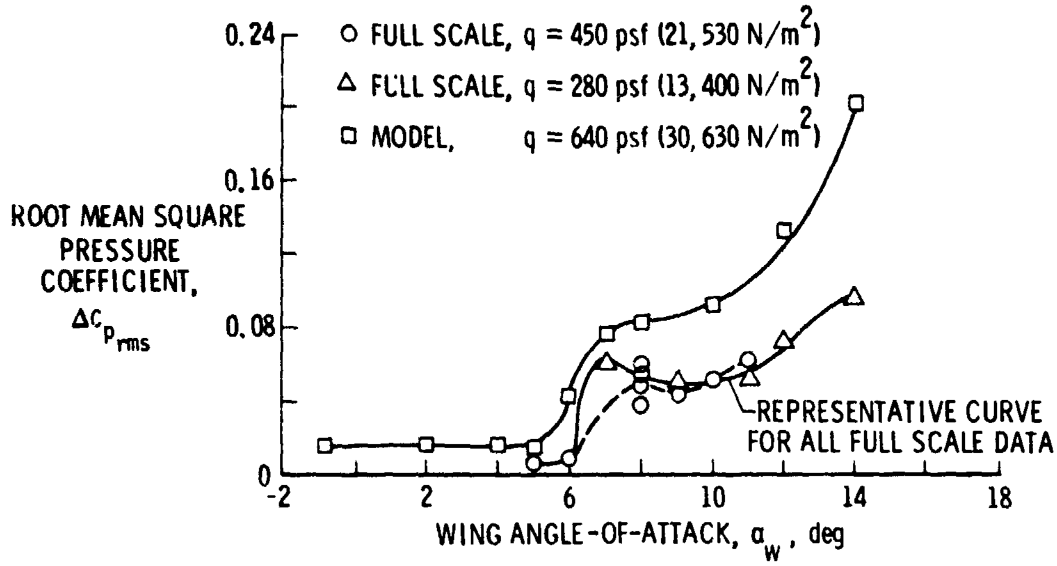
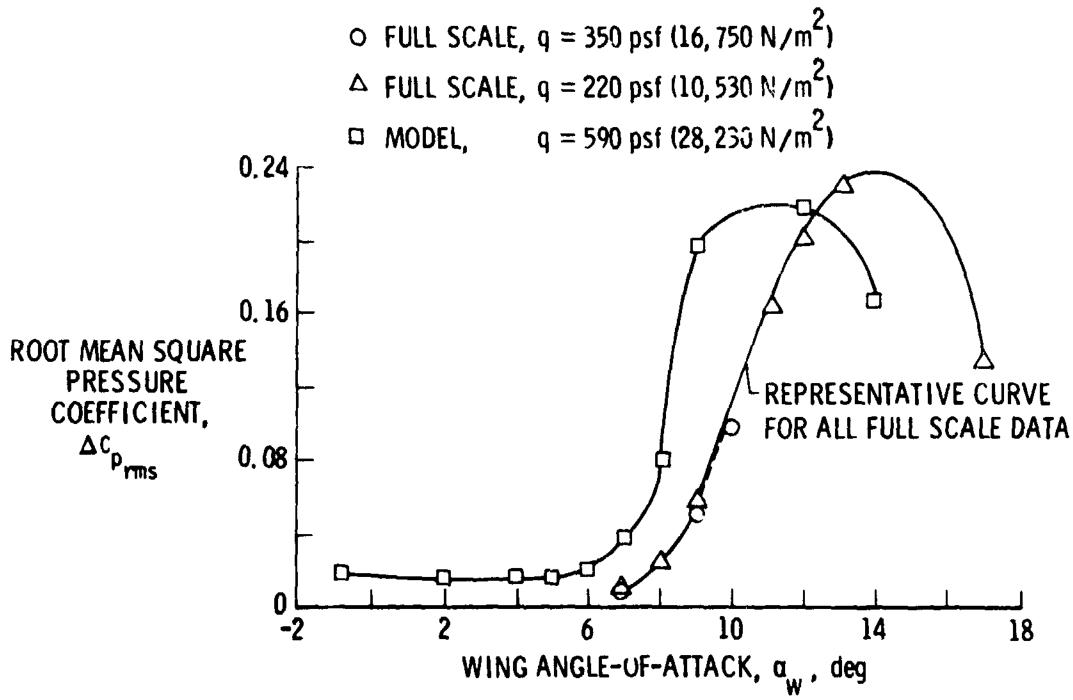


(a) M = 0.9, 90 percent chord, 86 percent semispan.

Figure 1. Comparison of model and full-scale rms pressure coefficient variation with angle of attack. (Ref. 9).



(b) M = 0.8, 90 percent chord, 78 percent span.

Figure 1. Concluded.

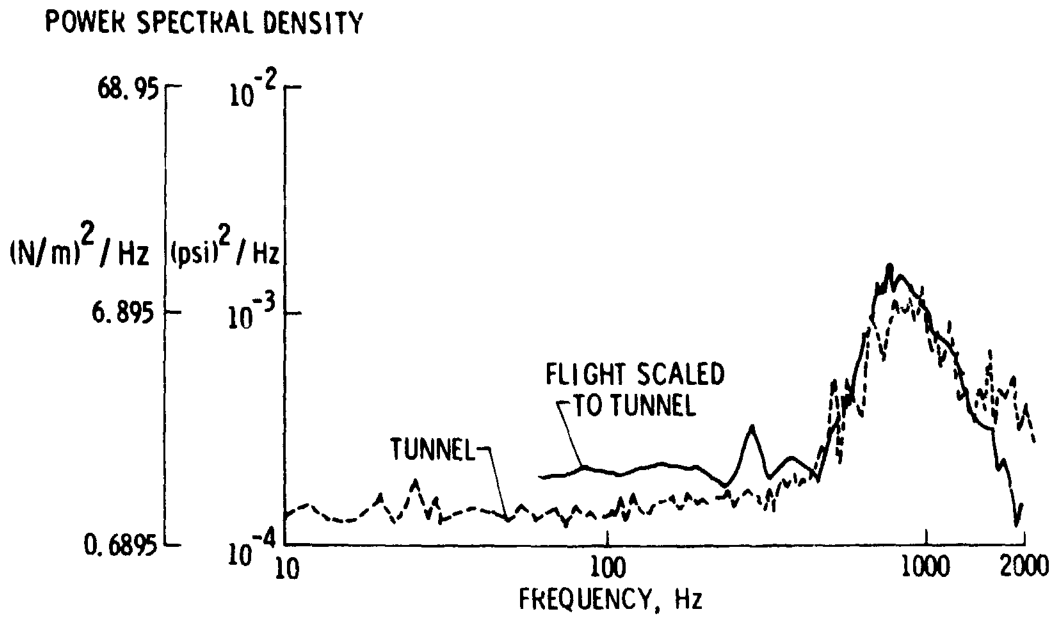


Figure 2. Comparison of model and full-scale pressure spectral shape (Mach 0.85, wing angle of attack 10°, transducer 18) (Ref. 9).

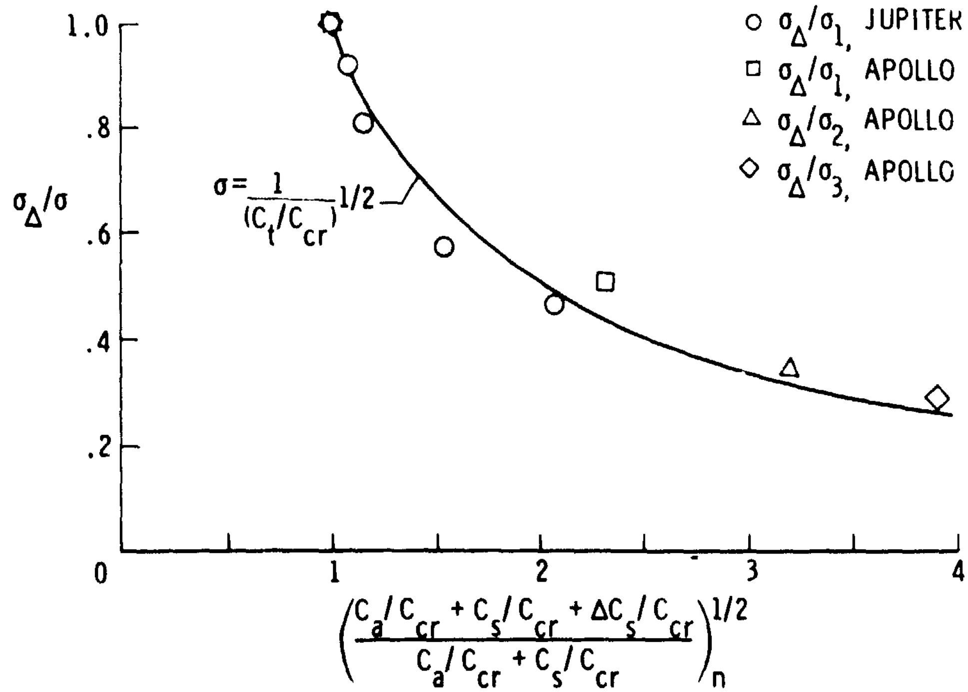


Figure 3. Effect of increase in structural damping on model buffet response at $M = 0.9$, angle of attack 0°, (Ref. 17).

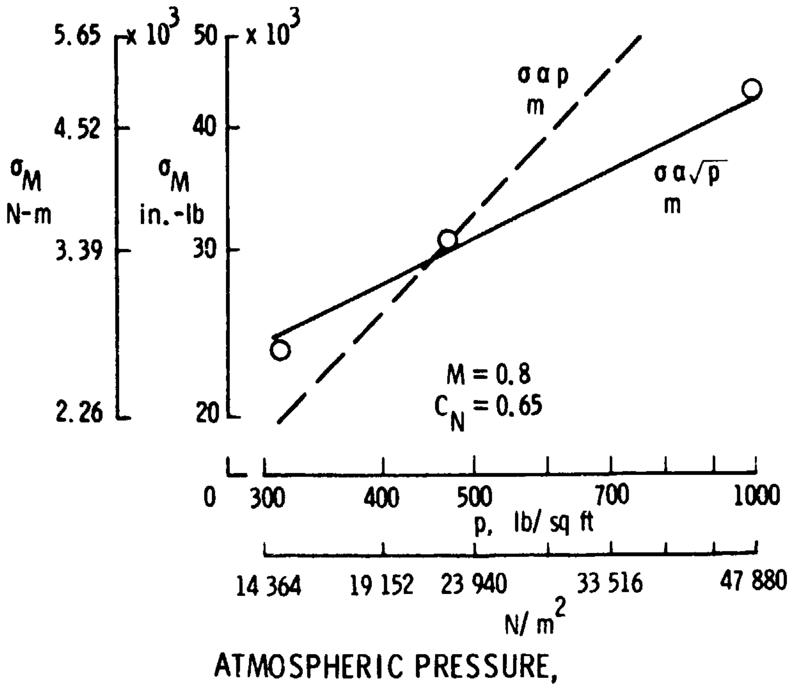
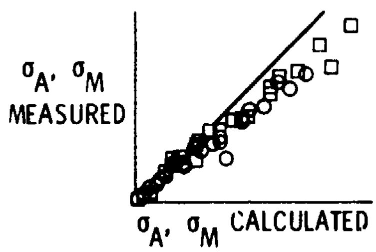
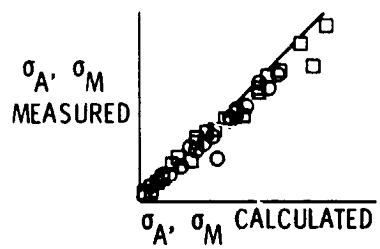


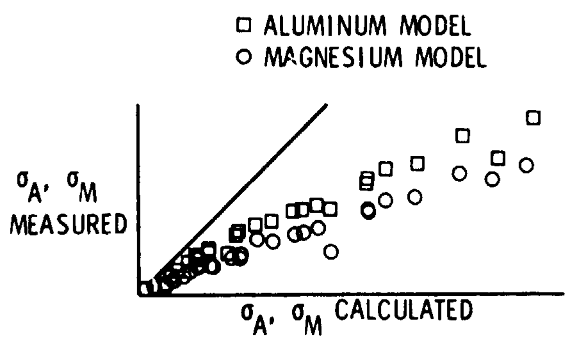
Figure 4. Variation of buffet loads with altitude. (Ref. 15.)



(a) Aerodynamic and structural damping.

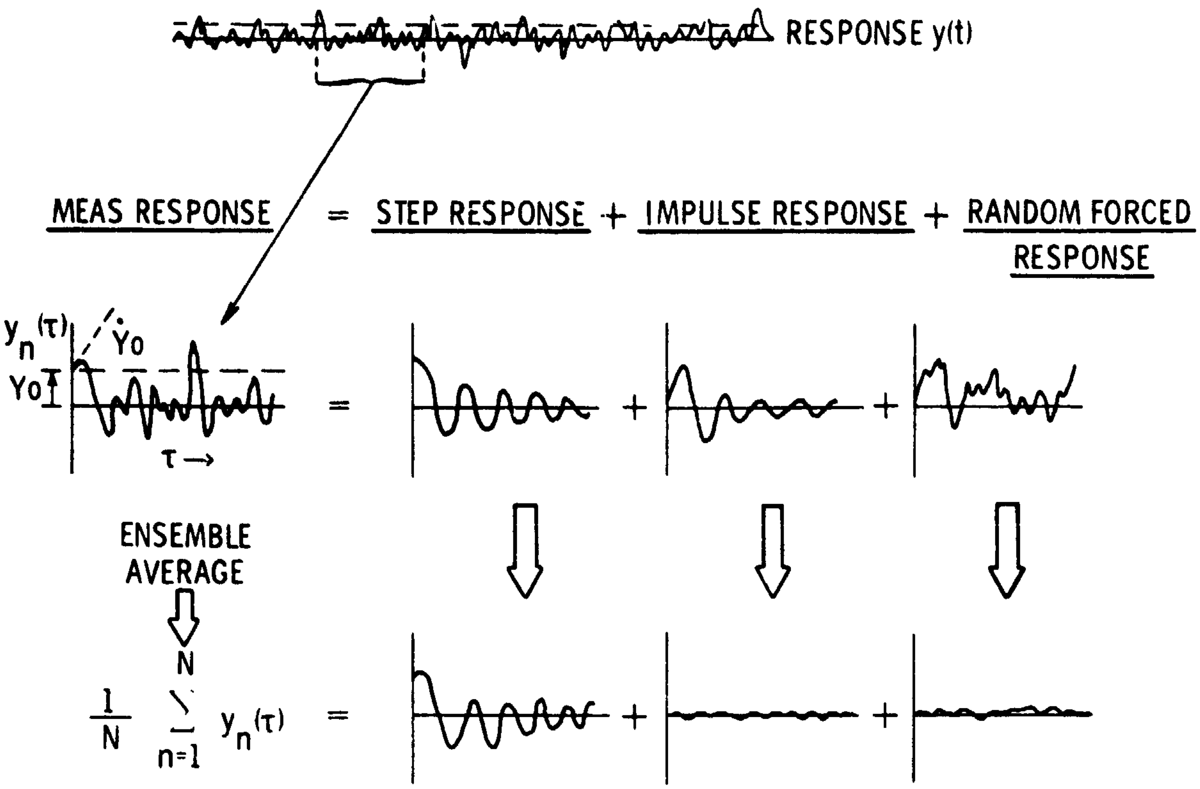


(b) Aerodynamic damping only.



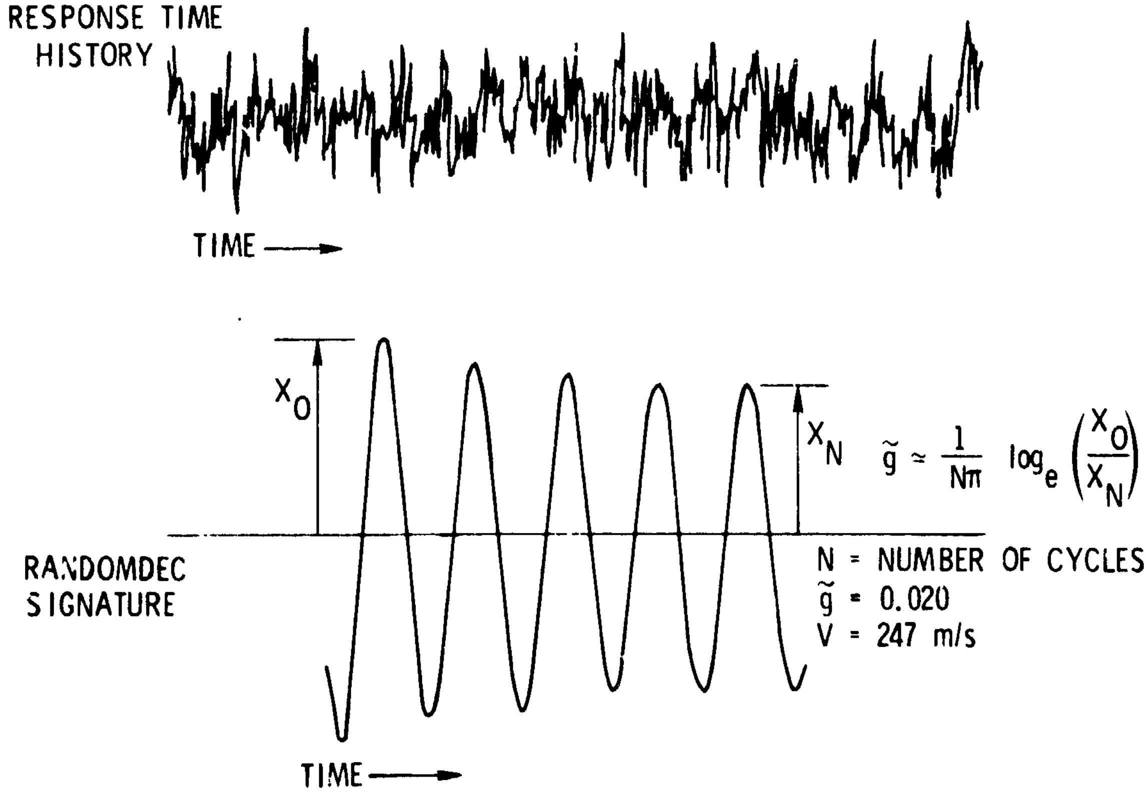
(c) Structural damping only.

Figure 5. Comparison of measured and calculated root-mean-square bending moments. (Ref. 16.)



(a) Basic concept.

Figure 6. Random-dec technique for determining damping. (Ref. 24.)



(b) Example of application.

Figure 6. Concluded.

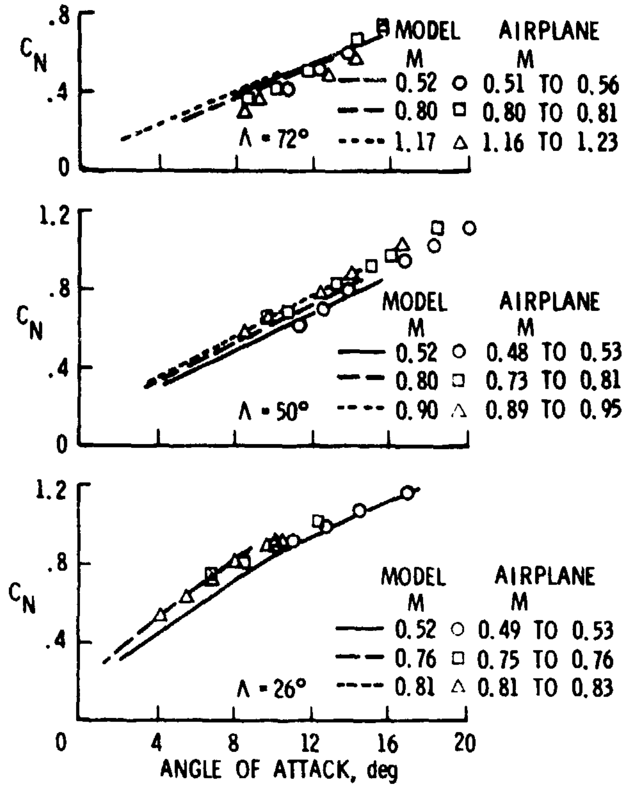


Figure 7. Comparison of model and airplane C_N variation with angle of attack. (Ref. 31.)

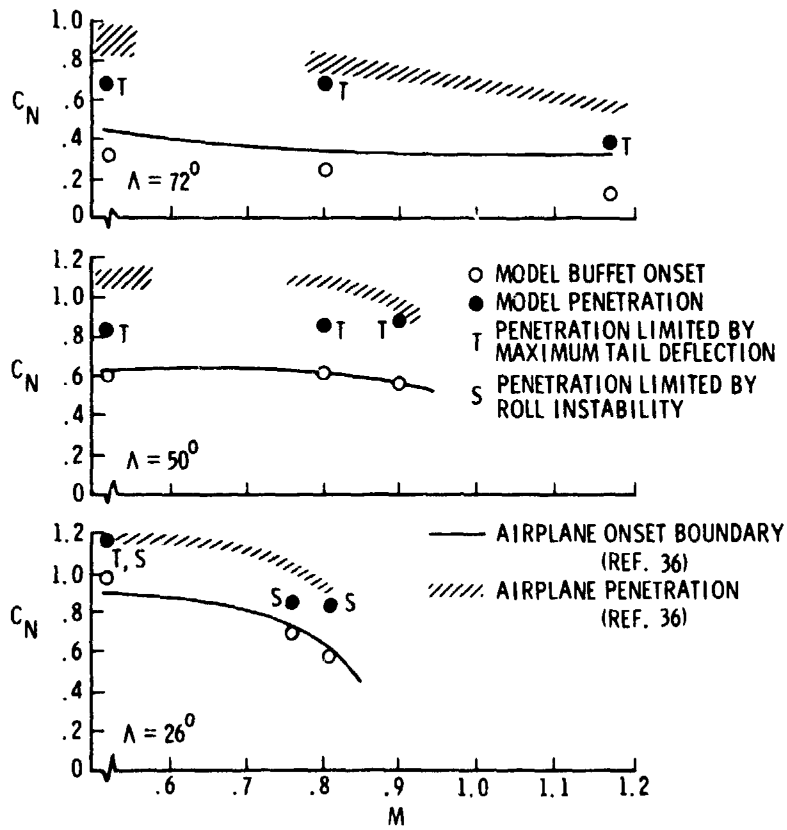


Figure 8. Comparison of model and airplane buffet onset and boundary penetration. (Ref. 31.)

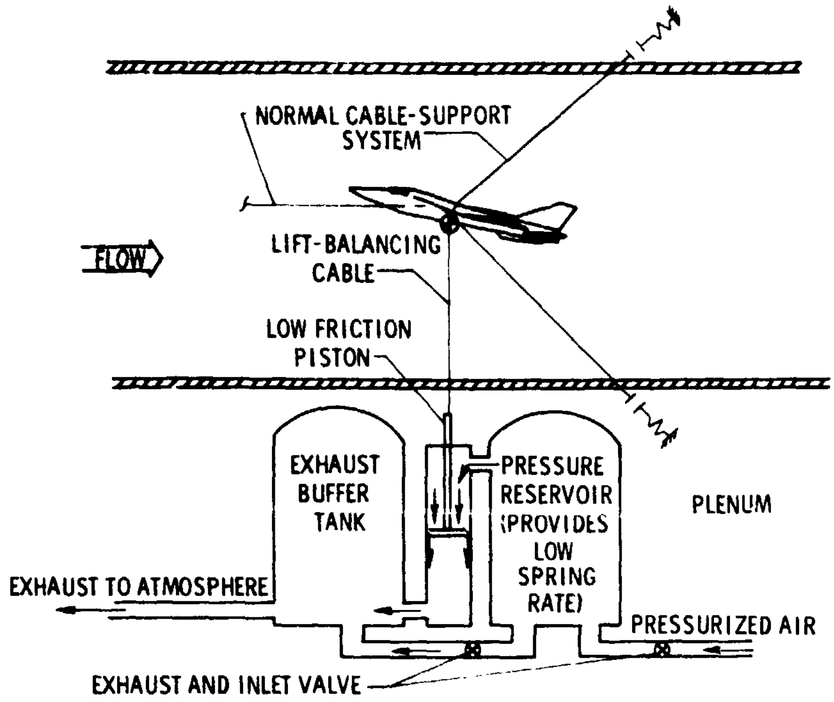
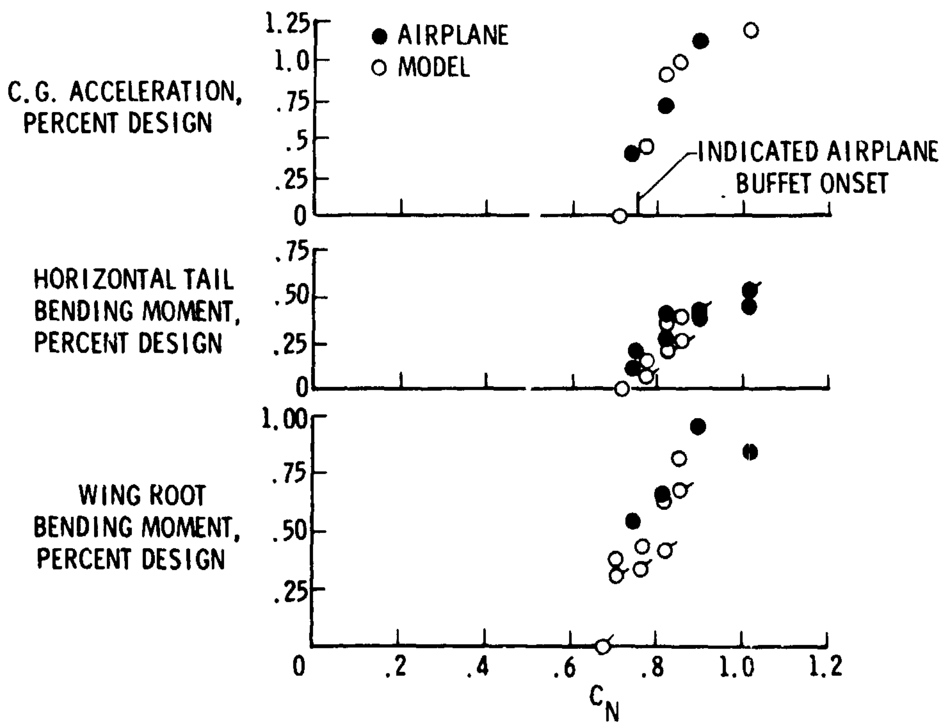
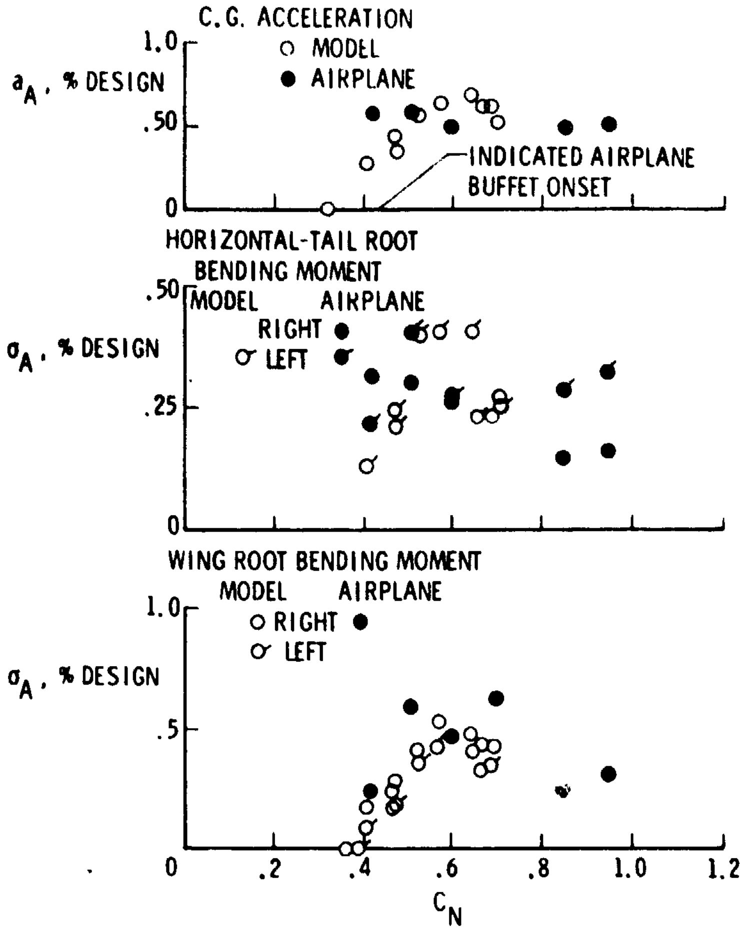


Figure 9. Lift-balancing device for testing models at high load factors. (Ref. 31.)



(a) M_M = 0.76; M_A = 0.76 to 0.75; 26° wing sweep.

Figure 10. Comparison of airplane buffet response and buffet response predicted from model data normalized on airplane design loads. (Ref. 31.)



(b) M_M = 0.52; M_A = 0.56 to 0.51; 72° wing sweep.

Figure 10. Concluded.

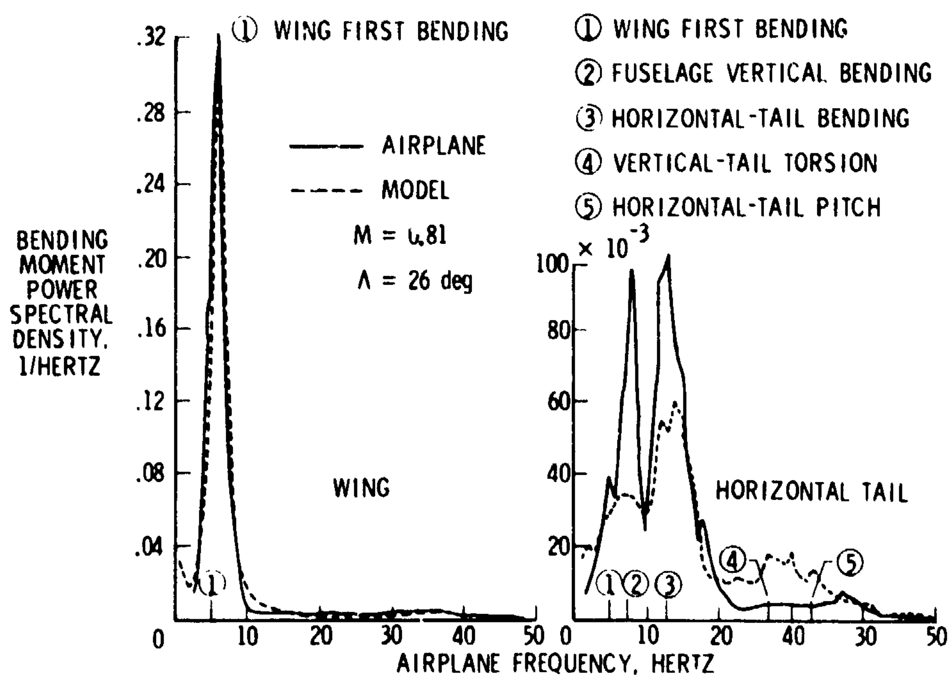
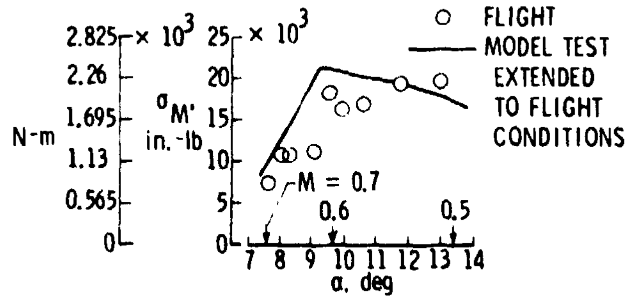
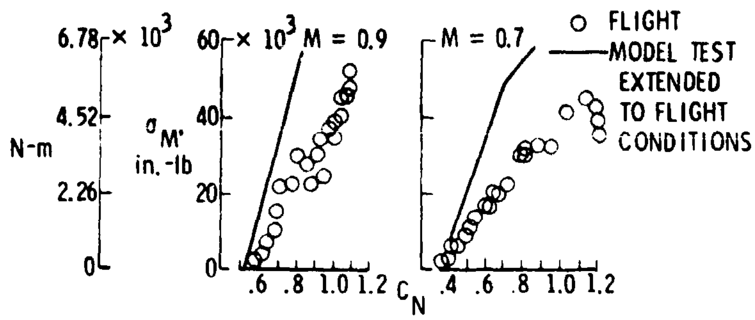


Figure 11. Sample comparisons of model and airplane response spectra for wing and horizontal tail. M = 0.81, 26° wing sweep. Spectra normalized on rms level. (Ref. 31.)



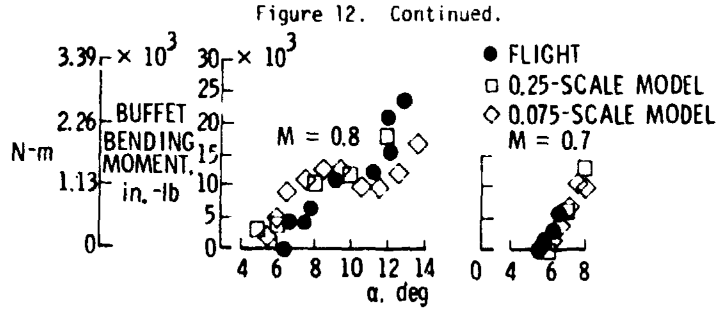
(a) X-3 airplane (Ref. 15).

Figure 12. Comparisons of measured and scaled buffet loads.



(b) D-558-II airplane (Ref. 1‥

Figure 12. Continued.



(c) X-1E airplane (Ref. 40).

Figure 12. Concluded.

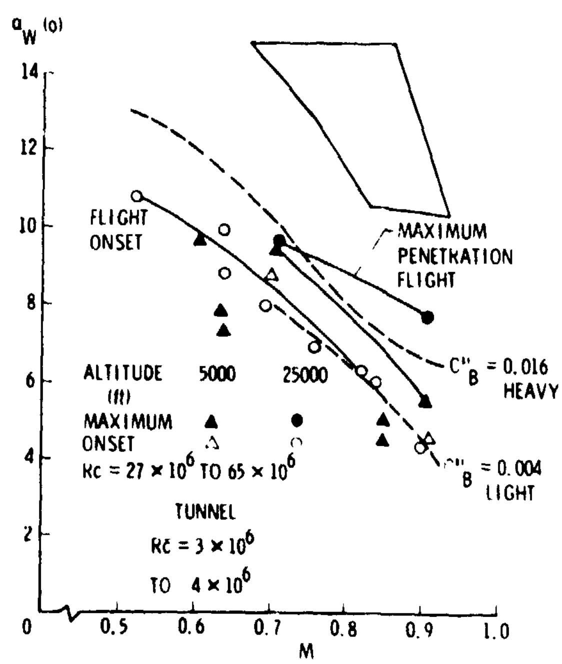
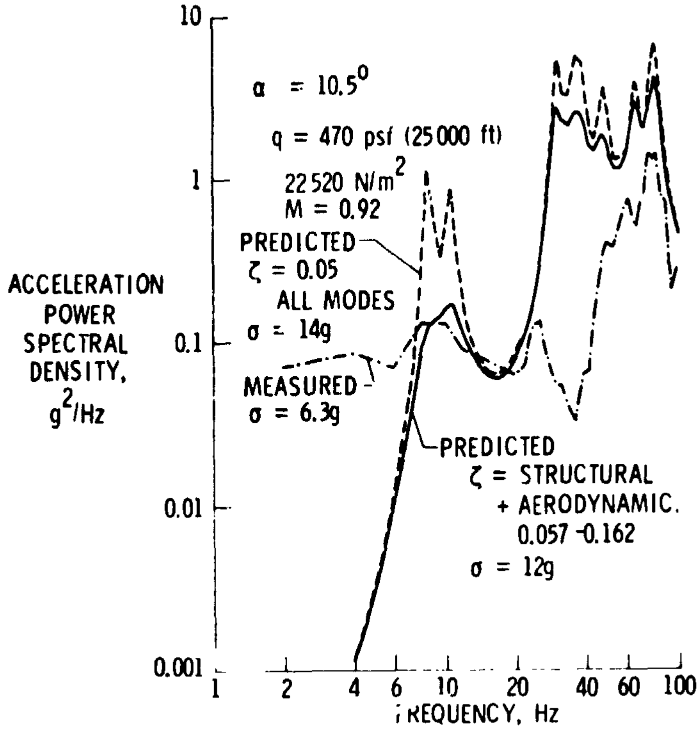
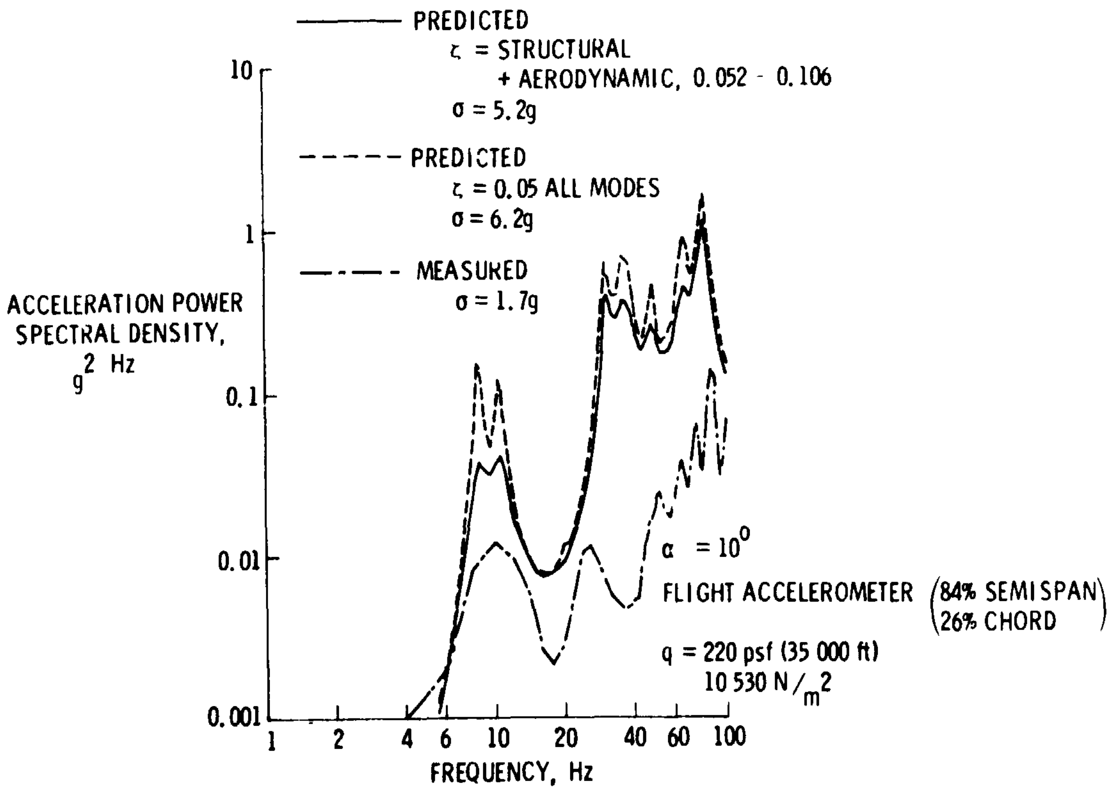


Figure 13. Aircraft buffeting penetration boundaries and model buffeting contours. (Ref. 41.)



(a) Mach 0.92.

Figure 14. Comparison of predicted and measured acceleration response. (Ref. 42.)



(b) Mach 0.79.

Figure 14. Concluded.