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Inlet Drag Prediction for Aircraft Conceptual Design

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Recent efforts to upgrade the aircraft conceptual design code ACSYNT prompted a study of inlet drag prediction methods suited to conceptual design. Existing methods were expanded to enable the drag of four different inlet types (subsonic pitot, supersonic pitot, supersonic two-dimensional, and supersonic conical) to be predicted over the complete inlet operating range. These methods have been successfully incorporated into ACSYNT and are presented together with test cases representing a range of inlet geometries.

Nomenclature

A = area
$C_{D_{add}}$ = additive drag coefficient
$C_{D_{DF}}$ = disturbed flow drag coefficient
$C_{D_{inlet}}$ = inlet drag coefficient
$C_{D_{lip}}$ = lip wave drag coefficient
$C_{D_{NS}}$ = normal shock drag coefficient
$C_{D_{prof}}$ = cowl profile drag coefficient
C_{D_s} = cowl suction coefficient
$C_{D_{spill}}$ = spillage drag coefficient
$C_{D_{wave}}$ = wave drag coefficient
C_f = skin friction coefficient
D = cowl diameter
F_f = force on forebody
f_f = form factor
f_r = fineness ratio
K = coefficient in lip drag formulation; coefficient in form factor
K_{add} = factor used to determine spillage drag
K_0 = coefficient in form factor
L = cowl length
L_2 = bow shock position
M = Mach number
P = static pressure
q_∞ = freestream dynamic head, $\frac{1}{2}\gamma P_\infty M_\infty^2$
t/c = thickness-to-chord ratio
α = factor used to determine spillage drag
β = factor used to determine spillage drag; exponent in shock expansion method
γ = ratio of specific heats
θ = forebody wedge angle (two-dimensional inlet); cone semiangle (conical inlet)
λ = flow angle at inlet throat
ψ = exponent in shock expansion method

Subscripts

c = inlet capture face; cone segment
f = forebody
lip = cowl lip
m = maximum cowl diameter
n = nth cowl segment
s = cowl surface; cowl segment
t = inlet throat
0 = total
2 = immediately downstream of normal shock
∞ = freestream

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Introduction

THE computer code ACSYNT was originally developed at the NASA Ames Research Center in the early 1970s for aircraft conceptual design, synthesis, and optimization. It was first written for batch-mode operation and required the tedious preparation of lengthy input data files. Such approaches are outdated in the present era of powerful graphics workstations and three-dimensional graphics standards. Consequently, recent enhancements made by the CAD Laboratory at VPI & SU in collaboration with the NASA Ames Research Center have greatly improved the processes of geometric modeling, data input, and graphical postprocessing for ACSYNT. The subsequent sponsorship of ACSYNTs development by many U.S. aerospace companies and research centers[1] has provided the impetus for the code to become a de facto industry standard.

Concurrently with the graphical advancements, it has become necessary to update the aerodynamic analysis within ACSYNT so that the effects of adding new geometric features to an aircraft design will be suitably reflected in its performance synthesis. Since ACSYNT is used at the conceptual design level, the aerodynamic analysis should not be too computationally expensive, particularly when the code is performing optimization. This imposes certain complexity and accuracy limitations. More important than accuracy, though, is the requirement that the analysis should properly reflect the performance penalty trends inherent in any design choice. Also, the entire operating range of the aircraft should be covered, avoiding discontinuities that may adversely affect the optimizer.

Since inlet drag can exceed 20% of the total aircraft drag under certain flight conditions, it must be appropriately synthesized with other propulsion system installation penalties. Although propulsion system installation penalties have long been evaluated with the aid of computer programs, these codes are often unwieldy, many require proprietary data, and they are generally intended for operation by propulsion specialists. Being a conceptual design code, ACSYNT originally contained a simplified additive drag analysis for one inlet geometry (a supersonic inlet equipped with a conical forebody, combined with a simplistic profile and wave drag analysis of a podded engine nacelle).[2] The aim of this study was to extend this analysis to accommodate a wider range of inlet geometries.

During the past few decades, a wealth of data and methods pertinent to inlet design were published, many of them included in excellent texts.[3,4] By incorporating these valuable resources into ACSYNT, the level of the inlet drag analysis has been extended beyond the first-order correction normally used in conceptual design studies, without placing unrealistic demands on the designer. This enables the effect of inlet geometry on aircraft performance to be more realistically reflected. To this end, new routines were developed that are capable of predicting the drag of four different inlet geometries: 1) a subsonic axisymmetric pitot inlet, 2) a supersonic axisymmetric pitot inlet, 3) a supersonic axisymmetric inlet with a conical forebody, and 4) a supersonic two-dimensional inlet with a wedge forebody. The subsonic pitot inlet is assumed to be equipped with a cowl profile of the NACA 1-Series family, while the other inlets are all assumed to have parabolic profiles. A further assumption is that the forebody-equipped supersonic inlets are two-shock (external compression) inlets. These four basic inlet types represent a foundation upon which prediction methods for more sophisticated inlet types can be constructed in the future.

In this article, methods of estimating the drag of the four candidate inlet types are briefly described, and test cases reflecting more advanced inlet geometries are presented. While most of the design correlations are too lengthy to reproduce in this article, the sources of the methods and correlations referenced are readily available. A more extensive review, including curves fitted to correlations previously presented only in graphical form, is given by Malan,[5] together with the relations used to estimate the additional propulsion system installation penalties within ACSYNT.

Drag Nomenclature

The thrust/drag accounting system within ACSYNT obtains the installed thrust by charging the propulsion system drag to the uninstalled thrust. Using a component buildup method, the propulsion system drag comprises nozzle drag, inlet drag, and nacelle drag. The inlet drag in turn consists of spillage, bleed, bypass, and environmental control systems drag, while the nacelle drag consists of wave and profile drag for the entire nacelle.

In this article, the inlet drag is defined as the sum of the spillage drags and the fractions of nacelle wave and profile drags associated with the inlet. This is a convenience that allows the primary effects of geometry on the drag of an isolated inlet to be considered. It does not detract from the drag analysis presented, since the relevant drag components can obviously be rearranged to suit any thrust/drag accounting system. Furthermore, any of the methods may be replaced by an alternative method if so desired. For instance, the cowl wave and profile drag calculations could be incorporated into the total aircraft wave drag estimate instead of using the methods presented herein. However, having an independent inlet drag estimate permits a preliminary optimization of the component, and also enables the inlet drag prediction techniques to be validated.

For the present purposes, the inlet drag coefficient is defined as

$$C_{D_{\text{inlet}}} = C_{D_{\text{spill}}} + C_{D_{\text{wave}}} + C_{D_{\text{prof}}} \tag{1}$$

where $C_{D_{\text{spill}}}$, $C_{D_{\text{wave}}}$, and $C_{D_{\text{prof}}}$ are the spillage, wave, and profile drag coefficients for the inlet, respectively. Each of these coefficients corresponds to the respective drag force nondimensionalized by the product of q_∞ and the inlet capture area A_c. The numerical values of these coefficients are greater than or equal to zero, and are functions of the inlet operating conditions.

Spillage drag is the drag force associated with the excess air spilled over the cowl whenever the mass flow ratio A_∞/A_c is less than unity (see Figs. 1 and 2). ($A_\infty/A_c < 1$ implies that the engine mass flow demand is less than the maximum that could be accommodated by the inlet at that flight Mach number.) The concept of additive drag is fundamental to the estimation of spillage drag, and this is defined in the following section that deals with additive drag calculation methods.

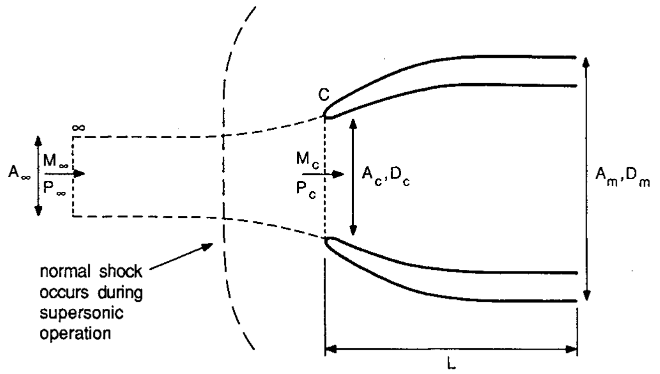

Fig. 1 Pitot inlet nomenclature.

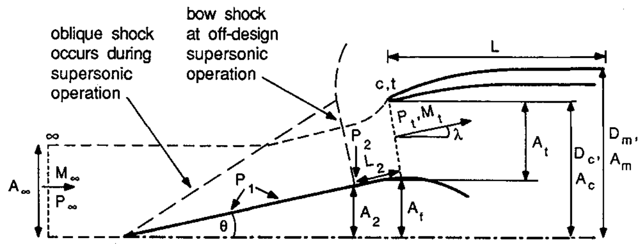

Fig. 2 Forebody inlet nomenclature.

Profile drag is the combination of skin friction and form drag (form drag being the result of a nonzero pressure gradient over the profile), and wave drag is the increased pressure drag due to the advent of localized or global supersonic flow. Both are calculated for the full-flow condition, $A_\infty/A_c = 1$. The deviation from the full-flow drag, known as cowl suction, is implicitly estimated as part of the spillage drag.

Additive Drag Calculation Methods

The significance of the additive drag coefficient is best seen from its definition:

$$C_{D_{\text{add}}} = \frac{1}{q_\infty A_c} \int_\infty^c (P - P_\infty)\, dA \tag{2}$$

This equation shows that the additive drag represents the total streamwise component of the pressure force acting on the inlet capture streamtube. The concept of additive drag arises from the definition of installed thrust and, if the flow is ideal and undisturbed as it passes over the surface of the nacelle, the additive drag will be completely canceled by the cowl suction. In actuality, the flow around the cowl is disturbed and not ideal so that viscous and compressibility effects inhibit the additive drag cancellation, and this must be accounted for.

The term "spillage drag" is therefore used to describe that portion of the additive drag which is not recovered by the cowl suction. Since it is generally an accepted practice to express the spillage drag as a function of the additive drag, $C_{D_{\text{add}}}$ must generally be calculated as a precursor to the spillage drag coefficient.

Pitot Inlets

To evaluate the additive drag coefficient given by Eq. (2) for pitot inlets, a mass and momentum balance is applied across a control volume bounded by a station far upstream, the inlet capture face, and the bounding stream tube (Fig. 1). Assuming one-dimensional flow, this yields

$$C_{D_{\text{add}}} = \frac{[P_c \gamma M_c^2 + (P_c - P_\infty)]A_c - P_\infty \gamma M_\infty^2 A_\infty}{q_\infty A_c} \tag{3}$$

For subsonic operation, isentropic flow relations[6] may be used to calculate P_c and M_c. During supersonic operation, a normal shock occurs between the upstream station and the inlet plane. The pressure and Mach number downstream of the normal shock can be evaluated with normal shock relations,[6] and P_c and M_c may be estimated by assuming isentropic flow between the shock and the inlet capture face.

The results of Eq. (3) are shown graphically by Leyland[7] for a range of freestream Mach numbers and mass flow ratios.

Supersonic Forebody Inlets

The control volume analysis of $C_{D_{\text{add}}}$ for supersonic forebody inlets proceeds just as with pitot inlets. In this case, however, the control volume also has the forebody surface as one of its boundaries (Fig. 2). The resulting expression for the additive drag coefficient is

$$C_{D_{\text{add}}} = \frac{F_f + [P_t \gamma M_t^2 + (P_t - P_\infty)]A_t - P_\infty \gamma M_\infty^2 A_\infty}{q_\infty A_c} \tag{4}$$

The additional term in this expression, F_f, must be evaluated in a manner appropriate to the freestream Mach number. Here, the suggestions of Leyland[7] and Petersen and Tamplin[8] are followed.

1) For *subsonic operation*, the conditions at the inlet throat (which coincides with the capture plane since it is a two-shock inlet) are calculated as with the pitot inlet, assuming one-dimensional isentropic flow. The forebody force is assumed to be

$$F_f = [\tfrac{1}{2}(P_\infty + P_t) - P_\infty]A_f \tag{5}$$

Implicit in Eq. (5) is the rather gross assumption that the mean pressure on the forebody is the arithmetic average of the freestream pressure and the pressure at the throat.[7,8] This assumption is made in the absence of a more refined weighted average.

2) For *transonic operation*, the detached forebody shock is assumed to be a normal shock. The conditions at the inlet throat are evaluated by assuming isentropic one-dimensional flow between this detached shock and the throat. The mean forebody pressure is taken to be the arithmetic average of the pressure immediately downstream of the shock P_2 and the throat pressure P_t,[8] so that

$$F_f = [\tfrac{1}{2}(P_2 + P_t) - P_\infty]A_f \tag{6}$$

3) During *supersonic operation*, an oblique (two-dimensional inlet) or conical (conical inlet) shock will form from the tip of the forebody. Additive drag arises from two sources: 1) the shock deflecting the bounding streamline at off-design Mach numbers, and 2) the formation of a bow shock causing a subsonic portion of the bounding streamline at mass flow ratios less than unity. In the supersonic region of the flow, the forebody pressure may be calculated using oblique shock relations[6] for the two-dimensional inlet, or obtained from tabulated data[9] for the conical inlet. The ramp pressure in the subsonic region is assumed to be the arithmetic average of the pressure immediately downstream of the bow shock and the inlet throat pressure.[8] Given the pressure at the throat P_t the pressure immediately downstream of the bow shock P_2, and the bow shock position L_2, the force on the forebody is approximated by

$$F_f = [\tfrac{1}{2}(P_t + P_2) - P_\infty](A_f - A_2) + (P_1 - P_\infty)A_2 \tag{7}$$

Due to the obvious differences in the flowfields, separate approaches must be taken to estimate P_t, P_2, and L_2 for the two-dimensional and conical inlet types. In both instances, the standard simplifying assumption is made that the forebody and bow shocks intersect outside the bounding streamline.

Two-Dimensional Inlet

The bow shock is generally assumed to be a normal shock. Therefore, the pressure immediately downstream of the bow shock is easily estimated using normal and oblique shock relations.[6] The throat pressure is obtained as before by assuming isentropic flow between the shock and the throat. Empirical relationships originally developed by Moeckel[10] and extensively modified by Osmon[11] are used to obtain L_2 as a function of Mach number and mass flow ratio, also considering the effect of sideplate geometry.

Conical Inlet

The conical flowfield presents special problems, since the Mach number in the region downstream of the forebody shock is nonuniform. Following a suggestion by Nicolai,[4] it is assumed that the forebody shock is a normal shock occurring at a Mach number that is the arithmetic average of the Mach number on the forebody cone surface and the Mach number immediately downstream of the conical shock. These Mach numbers can easily be obtained from the tabulated conical shock data of Sims,[9] and the throat conditions can be estimated by assuming isentropic flow downstream of the shock. The forebody L_2 must again be obtained from empirical relations, and Sibulkin[12] provides a simple correlation for this purpose.

For the conical inlet, these approximations do not necessarily result in zero additive drag at the inlet design point. To meet this criterion, the flow angle at the throat may be chosen to give an appropriate throat area. However, this approach requires an iterative solution because the throat Mach number and pressure are also functions of the throat area. A simpler approximation is to assume a throat flow angle based on geometrical considerations (e.g., $\lambda = \theta$) for estimating the throat conditions, and then use a slightly different throat area to ensure zero additive drag at design point. The minor inaccuracy thus introduced is generally negligible for a conceptual design analysis.

Spillage Drag Calculation Methods

The general approach taken is to express the spillage drag as a simple function of the additive drag. However, the forms of these functions differ according to inlet type, mainly due to the preferences of the workers who developed the functions from experimental data.

Subsonic Pitot Inlet

The simple formula proposed by Mount[13] is used to obtain the subsonic spillage drag from the additive drag:

$$C_{D_{\text{spill}}} = K_{\text{add}} C_{D_{\text{add}}} \tag{8}$$

This representation is criticized by Seddon and Goldsmith,[3] because K_{add} depends on cowl geometry, mass flow ratio, and Mach number. However, Mount[13] presents extensive correlations in graphical form derived from data accumulated from NASA and Rolls-Royce experiments on NACA 1-Series cowls. These correlations give K_{add} as a function of freestream Mach number, mass flow ratio, critical mass flow ratio, and cowl geometry. Since the NACA 1-Series cowl is intended primarily for low Mach number operation, transonic and supersonic data is scarce. Apparently there is minimal additive drag recovery for Mach numbers around 2 or greater, and the spillage drag coefficient varies approximately linearly with Mach number in the transonic range.[3] By fixing the mass flow ratio and allowing the spillage drag to vary linearly from its estimated value at Mach 1 to the additive drag at Mach 2 for that flow ratio, excellent agreement with the sparse experimental data presented by Seddon and Goldsmith[3] is achieved. The prediction of the critical mass flow ratio (i.e., the mass flow ratio below which the subsonic spillage drag becomes significant) can be derived from a correlation based on the cowl geometry.[3,14]

Supersonic Pitot Inlet

In the absence of extensive data for slender-cowled pitot inlets, a method suggested by Seddon and Goldsmith[3] is used.

The additive drag coefficient is divided into *disturbed flow* and *normal shock* drag coefficients. The disturbed flow drag is the drag associated with the separation of the flow at the cowl lip. In subsonic flow, this is equal to the additive drag, while in supersonic flow, the disturbed flow drag coefficient is

$$C_{D_{\mathrm{DF}}} = \frac{[P_c \gamma M_c^2 + (P_c - P_2)]A_c - P_2 \gamma M_2^2 A_\infty}{q_\infty A_c} \tag{9}$$

where M_2 and P_2 represent the conditions immediately downstream of the (normal) bow shock.

The normal shock drag is related to the pressure rise across the bow shock, and is the difference between the additive drag (3) and disturbed flow drag (9) in a supersonic flow. The normal shock drag coefficient may be written simply as

$$C_{D_{\mathrm{NS}}} = \frac{(P_2 - P_\infty)(A_c - A_\infty)}{q_\infty A_c} \tag{10}$$

Taking a linear combination of (9) and (10), the spillage drag coefficient is written as

$$C_{D_{\mathrm{spill}}} = \alpha C_{D_{\mathrm{DF}}} + \beta C_{D_{\mathrm{NS}}} \tag{11}$$

where the coefficients α and β are correlated with Mach number and cowl initial slope from experimental data.[3] Seddon and Goldsmith[3] suggest that $C_{D_{\mathrm{DF}}}$ be neglected for Mach numbers greater than 1.8. However, because this quantity decreases asymptotically to zero at high Mach numbers, there is no need to explicitly neglect it. The result does not differ significantly from the original formulation, yet maintains a continuous prediction compatible with ACSYNTs optimization routine.

Supersonic Two-Dimensional Inlet

The spillage drag coefficient for the supersonic two-dimensional inlet is obtained by subtracting the cowl suction coefficient from the additive drag coefficient:

$$C_{D_{\mathrm{spill}}} = C_{D_{\mathrm{add}}} - C_{D_s} \tag{12}$$

The coefficient C_{D_s} is obtained from the correlations derived by Osmon.[11] These correlations are presented as functions of cowl thickness-to-chord ratio, cowl lip radius, ratio of inlet capture area to cowl maximum area, mass flow ratio, ratio of inlet capture area to throat area, and throat Mach number for freestream Mach numbers of 0.692, 0.845, 1.093, 1.294, 1.393, and 1.687. The cowl suction coefficients for intermediate freestream Mach numbers may be obtained by interpolation. For Mach numbers outside the range given, the assumption is made that the cowl suction coefficient varies linearly to zero at Mach numbers of 0 and 2.

Supersonic Conical Inlet

In the absence of suitable experimental correlations, a spillage drag model for the supersonic conical inlet was based on the one used for the supersonic pitot inlet, since both inlet types were assumed to have sharp-lipped cowls with parabolic profiles. Using Eq. (11), the spillage drag for subsonic operation is simply

$$C_{D_{\mathrm{spill}}} = \alpha C_{D_{\mathrm{add}}} \tag{13}$$

For transonic operation, it is assumed that the detached forebody shock is analogous to the bow shock of the pitot inlet, although in reality it will not be a normal shock and it will cause the bounding streamline to deflect. A disturbed flow drag coefficient may then be derived as

$$C_{D_{\mathrm{DF}}} = C_{D_{\mathrm{add}}} - \frac{(P_2 - P_\infty)A_t + (P_\infty M_\infty^2 - P_2 M_2^2)\gamma A_\infty}{q_\infty A_c} \tag{14}$$

By assuming that $\beta = 1$ in Eq. (11) (the most conservative approach), the transonic spillage drag coefficient is then given by

$$C_{D_{\mathrm{spill}}} = (\alpha - 1)C_{D_{\mathrm{DF}}} + C_{D_{\mathrm{add}}} \tag{15}$$

For supersonic freestream flow, it is assumed that the sharp cowl lip will always result in separation, and that no cowl suction is recovered. In this case, the spillage drag will be equal to the additive drag.

Cowl Wave Drag Calculation Methods

Subsonic Pitot Inlet

A comprehensive source for the subsonic wave drag prediction of subsonic pitot inlets is an ESDU data sheet.[14] This reference provides a correlation for the drag-rise Mach number at the full flow condition as well as a further correction to adjust this prediction for varying mass flow ratios. Finally, a further correlation is provided for the prediction of subsonic wave drag as a function of freestream Mach number, drag-rise Mach number, and the additive drag estimated both at the freestream Mach number and the drag-rise Mach number.

Although this inlet type is primarily intended for subsonic operation, the design code might require an estimate of wave drag at transonic and supersonic Mach numbers. Due to the lack of experimental data and complexity of transonic flow solvers, a prediction method was sought that is not necessarily accurate, but will at least indicate to the designer when a supersonic flight Mach number incurs an excessive cowl drag penalty.

A method based on Newtonian theory is recommended by Seddon and Goldsmith[3] for the wave drag of a blunt circular-arc lip added to a slender cowl. The lip drag coefficient is given by the formula

$$C_{D_{\mathrm{lip}}} = [(KP_{0,2} - P_\infty)A_{\mathrm{lip}}]/q_\infty A_c \tag{16}$$

where $P_{0,2}$ is the total pressure downstream of a normal shock occurring at the freestream Mach number. Seddon and Goldsmith[3] provide an expression for K that is a linear function of the ratio of A_m to A_c. For the present study, the lip of the NACA 1-Series cowl is arbitrarily assumed to be that portion of the cowl which has a slope of less than 10 deg.

Supersonic Two-Dimensional Inlet

For supersonic Mach numbers where an oblique shock is assumed to be attached to the cowl lip at the full flow condition, the well-known two-dimensional shock-expansion method is employed. In this method, the cowl is divided up into discrete segments. Oblique shock relations enable the pressure on the first segment to be estimated, and then a Prandtl-Meyer expansion is assumed as the flow turns from one segment to another. Integration of these pressures enables the wave drag to be easily calculated.

For transonic Mach numbers, the cowl is approximated as a wedge, and transonic similarity laws[15] are used to derive a drag coefficient. An equivalent wedge angle, η_w for the curved cowl is taken to be the following weighted average:

$$\eta_w = (\eta_c + 2\eta_m)/3 \tag{17}$$

where η_c is the cowl initial slope, and η_m is the cowl slope that would be obtained if the cowl was wedge-shaped. This average was derived from numerical experiments, and might have to be adjusted for alternative cowl profile families. However, if linear weighting is applied to the transonic wave drag to ensure that it blends with the supersonic prediction without discontinuity, this helps to compensate for any errors introduced by the assumed equivalent wedge angle.

Supersonic Pitot and Conical Inlets

The modified shock expansion method of Syvertson and Dennis[16] is similar to the two-dimensional shock expansion method, except that the pressure after the initial expansion on each cowl segment P_s is allowed to fall exponentially to P_c, the pressure that would be experienced by a cone of the same local slope for the same Mach number. The simplest variant of the method is termed the "two-step" method, where the pressure distribution on the nth cowl segment P_n is approximated by

$$P_n = P_c + (P_s - P_c)e^{-\beta\psi} \tag{18}$$

where β is a function of cowl radius, distance along the cowl segment, and local cowl angle relative to initial cowl slope; and ψ is a function of local cowl angle, initial cowl angle, $(P_s - P_c)$, the Mach number downstream of the oblique shock, and the Mach number calculated by the Prandtl-Meyer expansion from the initial cowl angle to the local cowl angle. This method gives excellent agreement with full second-order theory as long as the cowl is curved. If the cowl is conical, however, the parameter ψ is zero, and the method reverts to first-order. This was not considered a serious problem for the present work, since the cowl shapes are assumed to be parabolic, and this method was therefore adopted as providing the best compromise between simplicity and accuracy.

Since shock expansion methods depend on the existence of an attached shock at the cowl lip, this method only holds for high Mach numbers (2 or greater) when the cowl slope is high (e.g., 20 deg). For this reason, the closed-form slender body solution of Willis and Randall for parabolic cowls as presented by Seddon and Goldsmith[3] is used for lower supersonic Mach numbers. This solution, in turn, has an upper bound on validity as the freestream Mach angle approaches the maximum slope of the cowl. Consequently, this solution is adjusted with a linear weighting function to ensure that the two solutions match in the Mach number region of overlapping validity.

In the absence of suitable theory or correlations in the transonic range, the slender body solution is used for Mach numbers close to unity. Subsonic drag rise is approximated by assuming a fixed drag-rise Mach number, and allowing the predicted drag to increase linearly from zero at the drag-rise Mach number to a peak at Mach 1.05. This peak is calculated with the Willis and Randall method for Mach 1.1, and is kept constant for $1.05 \leq M_\infty \leq 1.1$. The result is a transonic behavior that at least qualitatively resembles that of slender bodies.

Cowl Profile Drag

In each case, the profile drag coefficient is estimated by combining an appropriate form factor with a suitably estimated C_f, so that

$$C_{D_{\text{prof}}} = C_f f_f A_s / A_c \tag{19}$$

where A_s is the cowl surface (wetted) area.

Subsonic Pitot Inlet

For the subsonic pitot inlet, the following form factor is used:

$$f_f = (1/K_0)[1 + 0.33K(1 - D_c/D_m)D_m/L]^{1.667} \tag{20}$$

Here, K is a function of the mass flow ratio and the cowl area ratio A_m/A_c, and K_0 is a function of the cowl diameter and length-to-diameter ratios. Both K and K_0 are presented graphically in a data sheet by ESDU.[14]

Supersonic Two-Dimensional Inlet

For the supersonic two-dimensional inlet, a form factor commonly used for wing sections is employed

$$f_f = 1 + (t/c) + 100(t/c)^4 \tag{21}$$

where (t/c) is the cowl thickness-to-chord ratio.

Supersonic Pitot and Conical Inlets

The form factor contained in the original ACSYNT code is used for these inlet types

$$f_f = 1 + 3f_r^{3/2} + 7f_r^3 \tag{22}$$

where f_r is given by

$$f_r = \tfrac{5}{3}L/D_m + 1 \tag{23}$$

Results

The results of applying the methods described herein can, in general, be validated by the data from which they were derived. The question arises, however, as to how well the calculation methods will perform in the prediction of the drag of inlets other than those used as a basis for the development of the correlations.

Test cases consisting of the calculation of the inlet drag of a supersonic two-dimensional inlet, the inlet drag of a supersonic conical inlet, and the spillage drag of a supersonic pitot inlet are presented. The first two test cases were run with fixed values of the inlet dimensions and operating conditions. The third case was run as a part of a mission cycle analysis using ACSYNT to simulate the engine mass flow requirements.

Two-Dimensional Three-Shock External Compression Inlet

Drag data for a two-dimensional inlet with varying ramp configurations has been presented by Hawkins et al.[17] Although nominally a four-shock inlet, in its simplest configuration it performed as a three-shock inlet with ramp angles of 7 and 11 deg. This inlet was also equipped with a wedge-shaped lower cowl with a 4-deg wedge angle. The inlet was modeled by treating it as a two-shock inlet with a forebody ramp angle of 7 deg. This also gave an inlet throat area that differed from the test configuration.

The results of this calculation are shown in Fig. 3 for freestream Mach numbers of 0.85, 1.2, and 1.39 and a range of mass flow ratios. The results for $M_\infty = 0.85$ show very good agreement with the data, although the predicted slope of drag vs mass flow ratio relationship is higher than that of the test data. This is to be expected in all cases due to the difference in the throat area between the test and the calculated configurations. The fact that the magnitude of the predicted drag is satisfactory indicates that the combined profile and subsonic wave drag coefficients are in reasonable agreement with the experimental data.

The transonic case ($M_\infty = 1.2$) also exhibits good agreement, in spite of the simplifying assumptions made in the transonic additive drag analysis. As mentioned, the slope of the drag prediction is understandably higher than the data.

The supersonic case ($M_\infty = 1.39$) shows the poorest agreement with the experimental data. Unexpectedly, the slope of the drag prediction matches the data very well, but the magnitude is overpredicted by as much as 40%. This discrepancy is probably due to the favorable effect of the multiple forebody shocks on the spillage drag; an effect that is not captured by the calculation.

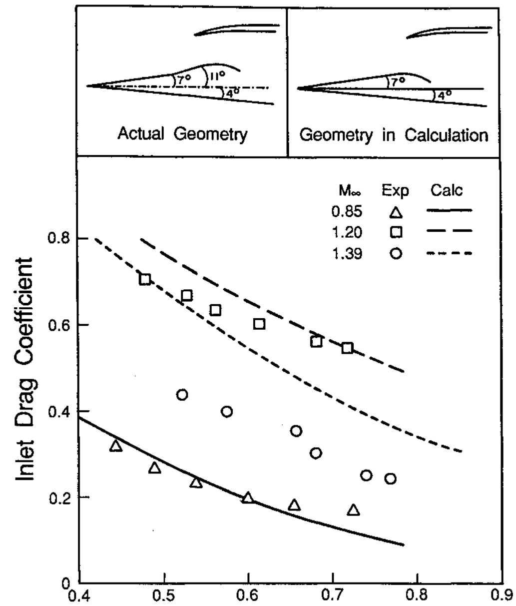

Fig. 3 Predicted inlet drag coefficient of a 3-shock, two-dimensional inlet. Compared to data of Hawkins et al.[17]

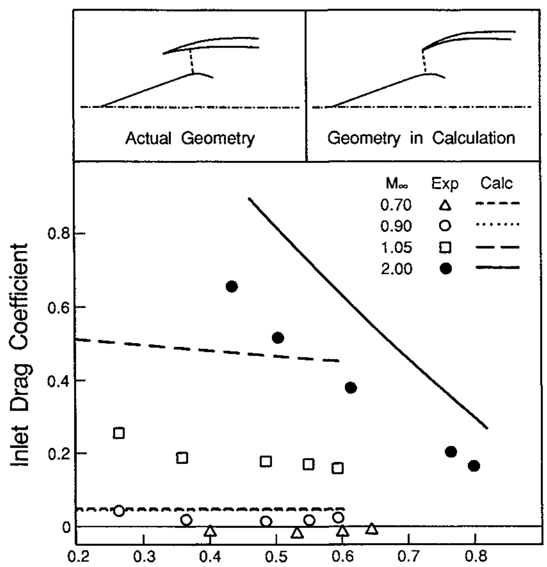

Fig. 4 Predicted inlet drag coefficient for a mixed-compression conical inlet. Compared to data of McVey et al.[18]

Conical Mixed-Compression Inlet

Inlet drag data for the tests on an axisymmetric, mixed-compression inlet with a design Mach number of 2.7 and a forebody cone angle of 20 deg are presented by McVey et al.[18] These tests were run at freestream Mach numbers of 0.7, 0.9, 1.05, and 2.00. In the present calculations the centerbody was moved from its actual position to produce an external-compression inlet deemed approximately equivalent to the original mixed-compression geometry. Figure 4 shows that for all Mach numbers the drag is greatly overpredicted. For supersonic flow, this overprediction can be explained in terms of the effect of the compression being mixed, which will reduce the displacement of the bow shock ahead of the inlet capture face. The discrepancy in the transonic case is most likely a result of the throat area not being properly represented by a two-shock inlet configuration.

The overprediction of the drag is not surprising, considering the differences between external and mixed-compression inlets. Most importantly though, the *trends* in drag variation

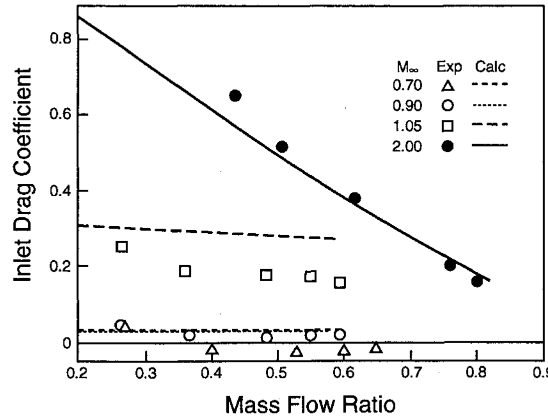

Fig. 5 Predicted inlet drag coefficient for a mixed-compression conical inlet, scaled by a factor of 0.6. Compared to data of McVey et al.[18]

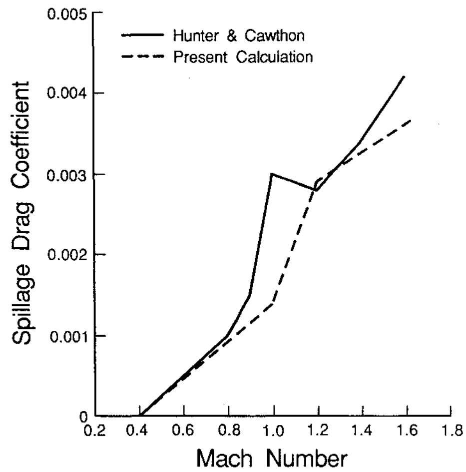

Fig. 6 Predicted spillage drag coefficient for the F-16 inlet. Compared to data of Hunter and Cawthon.[19]

are faithfully reproduced as the operating conditions of the inlet are varied. This is verified by simply scaling the drag prediction by a factor of 0.6, which results in an acceptable prediction as shown in Fig. 5. The value of this scaling factor is not significant since it will be geometry-dependent. Still, it is interesting to note that the drag of a mixed-compression inlet geometry can be reasonably well-approximated by applying a linear scale factor to the drag of a simpler two-shock inlet.

F-16 Inlet Spillage Drag

To test the effectiveness of the new methods within ACSYNT, the F-16 was synthesized, modeling the oval-shaped, chin-mounted inlet as a semicircular supersonic pitot inlet with a sharp cowl lip. The resulting spillage drag distributions are shown in Fig. 6 compared to the data of Hunter and Cawthon.[19]

The new inlet drag routines satisfactorily model the F-16 inlet, even though the exact geometry is not replicated in the analysis. The major discrepancy occurs in the transonic region, and it is likely that this is due to a weakness in the spillage drag model for this difficult-to-handle Mach number range. The disagreement at the design point of $M_\infty = 1.6$ is attributed to the uncertainty introduced by ACSYNTs inlet sizing routine.

Conclusions

Aircraft design methods are being continually refined to take advantage of increased computing power. One example of this is the continued effort to improve both the geometric modeling and the performance prediction capabilities of the integrated aircraft design code ACSYNT. This effort resulted in the present study, aimed at enhancing ACSYNTs inlet drag prediction capability.

Drag prediction methods suited to incorporation into a design code such as ACSYNT have been presented for four different inlet geometries: subsonic and supersonic pitot, supersonic two-dimensional, and conical. While this is not an exhaustive list of inlet geometries, it represents a substantial improvement over ACSYNTs previous capabilities. Work is currently underway to include mixed compression inlets.

The use of simplifying assumptions is an integral part of design. While the assumptions can often be justified based on experience or experiment, there are many situations in which they cannot. However, this does not necessarily detract from the analysis, because although accuracy is desirable, it is more important at the conceptual design stage that the prediction should at least exhibit qualitatively correct behavior over the full operating range of each inlet type. This requirement ensures that the designer will be alerted if a specific design choice results in an excessive drag penalty.

The test cases presented here are exceptionally demanding since they represent considerably more complicated inlet geometries (multishock forebodies and mixed compression inlets) than those for which the prediction methods were developed. In spite of this, the drag trends were captured satisfactorily.

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