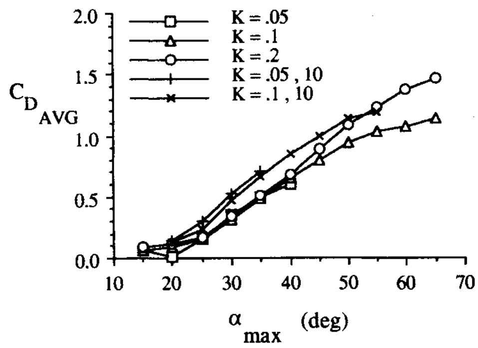

Fig. 4 Time-average drag coefficient results.

shows the average lift during the ramp-down motion is the dominant factor in the decline of the total average lift at high α_{max}. In the analysis in Ref. 1 a dynamic lift augmentation of $A_L = 1.5$ was assumed, and this resulted in an ALV with a near 20% improvement in straight-and-level range over the conventional baseline vehicle. In Fig. 3c, an augmentation of $A_L = 1.5$ was achieved at the highest pitch rate of $K = 0.2$ and $\alpha_0 = 0$ deg and for both cases of $K = 0.05$ and 0.1 for starting angles of $\alpha_0 = 10$ deg. The asssumption of Ref. 1 concerning the existence of augmentation parameters near 1.5 and above would appear then to have some justification.

Any benefits that might be gained from augmented lift must be weighed against the penalty levied by the increase in dynamic drag loading. Figure 4 shows the total time-average drag coefficients for each test motion. Figure 4 clearly shows that decreasing α_{max} is advantageous from the standpoint of average drag reduction. From Fig. 3c at the pitch rates of $K = 0.05$ and 0.1 and starting angles of 10 deg, a value of $\alpha_{max} = 35$ deg results in nearly optimum lift augmentation with respective A_L values of 1.5 and 1.63. The corresponding average drag coefficient is seen in Fig. 4 to be near 0.7, which by some standards is large. Notice, however, that the average drag decreases rapidly with decreasing α_{max}, and for $\alpha_{max} = 25$ deg and pitch rates of $K = 0.05$ and 0.1, the average drag coefficient has dropped to 0.3 and 0.24, respectively. At the same time, the lift augmentation remains well above unity and, from Fig. 3c, has values of 1.38 and 1.34, respectively. Thus for the rate of $K = 0.1$, decreasing α_{max} from 35 to 25 deg results in a drop in the average lift of 18% (though still maintaining significant lift augmentation), whereas the average drag decreases over 60%.

In the present study the pitch rate for the ramp-up and ramp-down motions was the same. For a stopping angle of $\alpha_{max} = 25$ deg the data of Fig. 3a indicate that the average lift during ramp up generally increases with pitch rate, whereas in Fig. 3b the average lift during ramp down decreases with pitch rate. There may then be some advantage in ramping up at a high rate followed by ramping down at a lower rate. In the motions studied in Ref. 1 the rate during pull-up was lower than that for pitch-down. Maintaining acceptable drag loading may be a limiting condition for defining airfoil motions for the purpose of utilizing augmented lift.

Acknowledgment

This work was supported by AFOSR Grant 87-0312.

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Optimum Cruise Lift Coefficient in Initial Design of Jet Aircraft

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Nomenclature

A = aspect ratio of wing
a = speed of sound at flight altitude
C = specific fuel consumption
C_D = drag coefficient
C_{Do} = parasite drag coefficient
C_L = lift coefficient
C_0 = parameter of specific fuel consumption in Eq. (10)
C_1 = parameter of specific fuel consumption in Eq. (10)
D = drag, $0.5\gamma pM^2SC_D$
K = range parameter
L = lift, $0.5\gamma pM^2SC_L$
M = Mach number
p = pressure at flight altitude
R = range
S = wing area
V = true cruise speed
W = airplane weight
β = exponent in the dependence of C with M
γ = ratio of specific heats of air
θ = temperature at flight altitude relative to sea level
ϕ = induced drag efficiency factor

Subscripts
cr = conditions at maximum range parameter
opt = conditions at maximum L/D

Superscripts
$*$ = condition of reference

Introduction

A VERY important point in the initial design of transport aircraft is the linkage between aerodynamic and propulsive parameters involved in the determination of the range. An airplane must be designed to fly in the surroundings of an appropriate optimum cruise condition, where fuel consumption is minimized, because of the economic impact of fuel cost in airplane operation,[1-3] and for the possibility of reducing in parallel the maximum takeoff weight, thus reaching the best design.[3,4]

In the classical analytical approach, the optimum cruise of a jet aircraft is usually considered with some simplifications, like no compressibility effects, constant specific fuel consumption,[5,6] and the constant altitude constraint imposed by air traffic control.[7,8]

Several attempts have been done to improve the former model and obtain a more realistic representation, providing complex empirical[1,7] or analytical relations[9]; most of them are not adequate for initial design.

In the present study, the specific fuel consumption is influenced by the Mach number, according to a potential law

Received July 23, 1990; revision received March 1, 1991; accepted for publication May 27, 1991.

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which, along with the constant altitude constraint, allows a simple analytical treatment of the range equation and related expressions, and the final result can be written in closed form.

Problem Formulation

Following classical flight mechanics, the range of a jet aircraft can be determined by means of Breguet's equation

$$R = \int \frac{V}{C}\frac{L}{D}\frac{1}{W}\,\mathrm{d}W \tag{1}$$

For a given amount of fuel, maximizing the range is equal, as a first approximation, to maximize the range parameter

$$K = \frac{V}{C}\frac{L}{D} \tag{2}$$

for each airplane weight during cruise.

As indicated in the introduction, this study considers that the specific fuel consumption varies with height and Mach number as

$$C = C^*(M/M^*)^\beta(\theta/\theta^*)^{1/2} \tag{3}$$

This expression is reasonably accurate for cruise conditions, as can be seen in Fig. 1.

On the other hand a parabolic polar will be assumed

$$C_D = C_{Do} + (1/\pi A\phi)C_L^2 \tag{4}$$

The errors involved in Eq. (4) are very small in long-range cruise, since this condition is not affected by drag divergence.[2,3,5,6]

If altitude is constant, maximizing the range implies

$$\frac{\partial}{\partial M}(M^{1-\beta}L/D) = 0 \tag{5}$$

The result obtained from Eq. (5) is

$$M_{cr}^2 = \left(\frac{3-\beta}{1+\beta}\right)^{1/2}\frac{W/S}{(\gamma/2)p(C_{Do}\pi A\phi)^{1/2}} \tag{6}$$

or, in lift coefficient

$$C_{Lcr} = \left(\frac{1+\beta}{3-\beta}\right)^{1/2}(C_{Do}\pi A\phi)^{1/2} \tag{7}$$

which for $\beta = 0$ coincides with the classical solution.

The optimum cruise takes place, then, at

$$K = \frac{aM^{*\beta}}{4C^*}\left(\frac{W}{(\gamma/2)pS}\right)^{(1-\beta)/2}((1+\beta)\pi A\phi)^{(1+\beta)/4} \cdot((3-\beta)/C_{Do})^{(3-\beta)/4} \tag{8}$$

As indicated in Fig. 1, the actual variation of specific fuel consumption is close to linear,[1,10] i.e.,

$$C = C_0 + C_1 M \tag{9}$$

and, consequently, maximizing the range could be expressed as

$$\frac{\partial}{\partial M}\left(\frac{M}{C_0 + C_1 M}\frac{L}{D}\right) = 0 \tag{10}$$

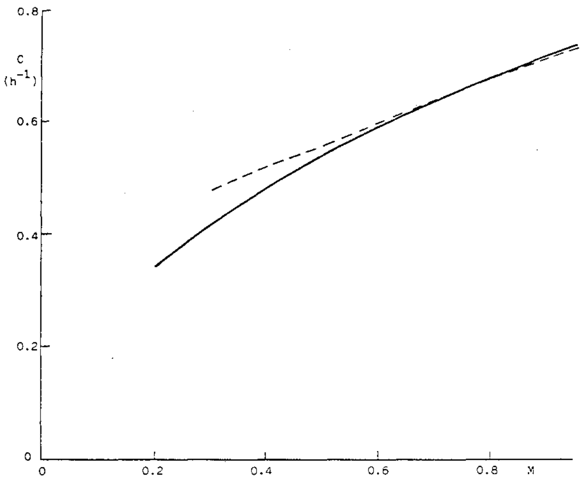

Fig. 1 Actual (dashed line) performance of a high bypass turbofan and approximation according to Eq. (3) as $C = 0.76\,M^{0.5}$.

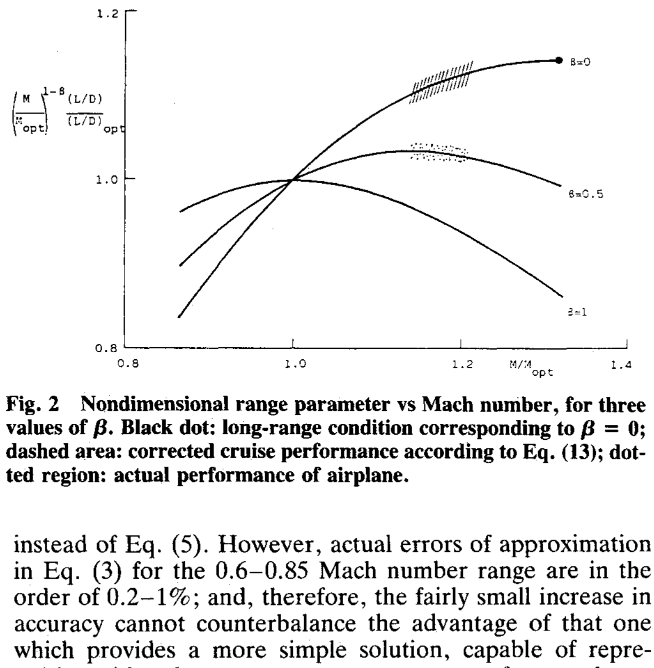

Fig. 2 Nondimensional range parameter vs Mach number, for three values of β. Black dot: long-range condition corresponding to $\beta = 0$; dashed area: corrected cruise performance according to Eq. (13); dotted region: actual performance of airplane.

instead of Eq. (5). However, actual errors of approximation in Eq. (3) for the 0.6–0.85 Mach number range are in the order of 0.2–1%; and, therefore, the fairly small increase in accuracy cannot counterbalance the advantage of that one which provides a more simple solution, capable of representing with only one parameter many types of powerplants.

Graphically, the influence of Mach number in the long-range condition can be illustrated as in Fig. 2. When $\beta = 0$ the picture depicts the classical pattern of pure turbojets: $M/M_{opt} = 1.32$ and $C_L/C_{Lopt} = 0.57$. In the opposite end, $\beta = 1$ resembles the case of turboprops. Actual turbofans have intermediate values of β: in the order of 0.2–0.4 for low-bypass engines, meanwhile modern high-bypass sets exhibit a 0.4–0.7 range (data obtained from engine manufacturers' brochures and initial aircraft design textbooks[3,6,11]). Torenbeek and Wittenberg,[9] in a fairly sophisticated treatment of aircraft performance, obtained an equivalent value to $\beta = 0.48$ in their numerical example.

Application to Performance Prediction

For a better understanding of the former development, consider the design of a hypothetical medium haul transport aircraft, with specified range and payload. To carry out the initial sizing, a few basic parameters are needed: namely, A, C_{Do}, and ϕ. They can be obtained from already existing similar airplanes, some databases, and so forth.[3,5,10] Let the appropriate figures be $A = 8$, $C_{Do} = 0.018$, and $\phi = 0.8$. This initial polar implies a maximum lift-to-drag ratio of $L/D_{opt} = 16.7$ at a lift coefficient $C_{Lopt} = 0.60$. Also, for the present analysis, a typical wing loading at cruise of 5000 Pa (100 psf) will be taken, equivalent to maximum takeoff wing loading around 6000 Pa.

Let us now apply the former values to two different situations, namely $\beta = 0$ and $\beta = 0.5$. Imposing a flight altitude of $h = 35,000$ ft and following Eqs. (6) and (7), the designer in the first case will try to fly at the typical cruise point where

$$M_{cr} = 0.93 \qquad C_{Lcr} = 0.35 \tag{11}$$

On the other hand, for the same long-range condition but $\beta = 0.5$, the second designer will find

$$M_{cr} = 0.80 \qquad C_{Lcr} = 0.47 \tag{12}$$

which are much closer to the common figures for medium haul transport airplanes. In this sense it must be recalled that Torenbeek[12] advises a first approximation of $0.17A^{1/2}$, here providing $C_{Lcr} = 0.48$.

It is reasonable to argue that the first designer will guess that he must shift flight conditions towards more common numbers, for example by increasing the aspect ratio of wing within realistic limits, say up to $A = 9.5$. The new long-range cruise would be at

$$M_{cr} = 0.89 \qquad C_{Lcr} = 0.38 \tag{13}$$

As shown here, the improvement is very poor and, moreover, any attempt to fly above $M = 0.8$ will undoubtedly find compressibility effects, counteracting the gain.

The implications of all these mistakes can be clearly described by means of Fig. 2. After abandoning the unrealistic top point, the first designer (the one assuming $\beta = 0$) believes that the airplane will operate on the dashed area, meanwhile the truth is that his aircraft is doing something worse, by some 7–10%, more or less around the pointed region since the specific fuel consumption is higher than expected.

Final Comments

Consideration of Mach number effects on specific fuel consumption during initial airplane design is important because these effects lead to improved accuracy and possibly to more realistic results; errors involved in the range parameter, through inappropriate values of β, are high enough for concern. On the other hand, these errors do not present an obstacle to the range equation which, as shown, can be treated and integrated analytically.

Two main assumptions have been made in the model: constant polar parameters and constant flight altitude. The first one seems to be an adequate hypothesis when flying in long-range conditions, since the Mach number is then well below the drag divergence value. Such simplification would not hold for high-speed cruise, but this is beyond the scope of the present study. Analogously, although the greatest fuel economy must be made through cyclic cruise and optimum trajectories, each cruise segment, obliged by air traffic control at constant altitude, could be analyzed in the above manner.

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Advanced Pneumatic Impulse Ice Protection System (PIIP) for Aircraft

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Introduction

THE modern pneumatic deicer is designed with a stretchable fabric-reinforced elastomer surface, specially compounded for weathering and erosion resistance. The surface distorts considerably when pressurized, serving to debond the ice under the combined influences of shear and peel, and to break the ice cap into pieces. The removal process is augmented by the scavenging airstream (see Fig. 1).

It is generally conceded that the principal advantages of a conventional pneumatic deicer, as compared to other systems, are its low weight, low cost, and adaptability and retrofitability to many types of aircraft, as well as the fact that it is a known, reliable performer.[1,2]

Despite the wide acceptance and long history of pneumatic deicers, some changes in design requirements have been suggested. For example, ice-sensitive airfoils and applications where engine ice ingestion are a concern generally require thin ice-shedding capability and small ice-particle shed size. In some cases ice as thin as 0.030 in. or as small as 0.25 in. equivalent particle diameter may be required to be shed. Since most mechanical ice removal systems shed thick ice more effectively than thin; the "thinness" of the ice becomes a measure of system performance. Requirements for rain erosion longevity and prolonged resistance to the elements for many applications may dictate a metal or plastic rather than an elastomeric surface. Finally, the fact that the typical pneumatic deicer is a "skin-bonded" article is often objectable; an aerodynamically nonintrusive system that allows the original airfoil design to be maintained is often required.

In 1984, an advanced type of pneumatic deicer was conceived at BF Goodrich. The desired improvements listed above became performance objectives for the new system.

Presented as Paper 90-0492 at the AIAA 28th Aerospace Sciences Meeting, Reno, NV, Jan. 8–11, 1990. Received March 17, 1990; revision received March 25, 1991; accepted for publication July 19, 1991.

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