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Some Recent Aerodynamic Advances in STOL Aircraft

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A brief review is presented of the results of some of the developments carried out recently by the de Havilland Aircraft of Canada Ltd., in two broad areas: 1) basic STOL performance, and 2) low-speed control and handling. In connection with basic STOL performance, the case for the classical STOL deflected slipstream configuration and some of its limitations are reviewed. Some evolutionary improvements to this basic design approach are outlined. In addition, the possibility of replacing the classical propeller installation with a true jet STOL aircraft is briefly discussed. Some improvements in longitudinal low-speed control are discussed. Other criteria are reviewed and some observed inherent limitations noted. Finally, the flight-test results obtained with an experimental aircraft incorporating a modified longitudinal control system are discussed.

Nomenclature

a_g = average ground deceleration, ft/sec²
C_D = drag coefficient
C_J = jet thrust coefficient
C_L = lift coefficient
C_{L_A} = approach lift coefficient
C_T = thrust coefficient
C_X = horizontal force coefficient
C_{X_A} = approach horizontal force coefficient
C_μ = blowing coefficient
q = dynamic pressure, psf
M = mass flow, slugs/sec
S = wing area, ft²
T_C = propeller thrust coefficient
T_P = propeller thrust, lb
T_J = jet augmenter thrust, lb
V_J = isentropic exit velocity, fps
V_V = vertical descent velocity, fps
α = angle of attack, deg
δ = aileron, flap elevator deflection, deg
φ = flight trajectory angle relative to horizon, deg

Subscripts

a = condition for attached flow
A = aileron
e = elevator
F = flap
0 = condition at freestream
disk = condition at propeller disk
ss = based on dynamic pressure in the slipstream

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1. Limitations of Conventional STOL Transport Aircraft

Existing propeller-powered STOL transport aircraft are characterized by a number of basic compromises that severely interact with the attainable performance levels. It will be of value to review briefly a few of the more important of these compromises.

Current STOL transport configurations normally combine a high level of static thrust with a somewhat oversized wing employing extensive high lift features. For the purposes of this paper and to establish a datum configuration against which certain aircraft improvements may be evaluated, those STOL configurations employing both a compromised (low) wing loading and thrust loading will be termed "conventional" STOL aircraft. Certain mandatory STOL characteristics (i.e., cargo density and deployment and an adequate rough field capability) significantly compromise the attainable aircraft performance. As a result, the conventional STOL transport vehicle exhibits the following major performance limitations:

1) Drag limitation. The basic vehicle characteristics, indicated previously, dictate the maximum degree to which ultimate aerodynamic cleanliness may be approached. The wing, fuselage, empennage, and nacelle drag areas are each sensitive to and increased by the STOL operational requirements.

2) Cruise limitation. A compromised minimum drag potential combined with the high level of installed thrust produces a limitation on the maximum cruise efficiency.

3) Structural efficiency limitation. Generation of the required low-speed lift and thrust compounded with the requirement for adequate stability and control at low forward velocity results in a rapid reduction in the obtainable structural efficiency (i.e., disposable load as a percentage of aircraft gross weight) as the STOL field requirements are increased. The reduction in structural efficiency is typically as shown in Fig. 1, which resulted from a design study with conventional twin-engine STOL aircraft at 40,000-lb A.U.W.

The conventional approach employed to provide an optimum vehicle in the light of the foregoing factors involves the use of large-diameter propellers combined with an efficient high-lift flap system. Two very significant low-velocity lift improvements are thereby realized. First, a lift improvement is achieved with the flaps deflected, resulting from the thrust deflection. In addition, a lift benefit occurs which is attributable to the boundary-layer control action achieved by the high total pressure air in the slipstream over a significant portion of the wing span outside of the slipstream proper. Both of these factors increase with flap angle and propeller thrust. During the takeoff maneuver, at maximum thrust level, both of these increments in lift are realized; however, since optimum STOL landings are performed at low or idle thrust levels, a substantial penalty in potential lift is implicit. Conventionally, the lift limitations encountered during the STOL landing maneuver amount to approximately 100% (i.e., the lift realized is approximately one-half of that available).

A further landing limitation inherent with the traditional slipstream deflection design arises as a result of the shape of full-flap drag polar. This polar is conventionally such that the landing approach is made at a point very close to minimum drag position. Thus the glide approach angle tends to be invariant with aircraft velocity, and a separate trajectory angle control is required. This control is normally provided by the propeller by means of a power adjustment. However, there are operational difficulties with such a control. These are discussed at some length in Sec. 4.

2. Evolutionary STOL Development

Three possible methods of improving the performance of the basic slipstream deflection configuration are briefly outlined and compared in the subsections below.

2.1 General Description of Methods

2.1.1 Full span slipstream coverage

Conventional STOL propeller transport configurations have tended to employ a minimum number of powerplants consistent with engine-out safety requirements and limited by the unit sizes available. Consequently, considerably less than 100% of the wing span is normally immersed in the propeller slipstream even with a four-engine installation. One possible method of improvement involves distributing the thrust and propellers along the wing span so that the complete wing is immersed in the slipstream. This scheme eliminates all of the wing area that is normally subject to the high induced propeller loading (i.e., the wing area in the freestream adjacent to the propeller).

2.1.2 Boundary-layer control

A second method of improving the effectiveness of the traditional propeller configuration is through the use of boundary-layer control to augment the lifting capability of the wing-flap combination. Analysis of system installation penalties (both weight and volume) has indicated a preference for the blowing boundary-layer control system employing a moderately high pressure ratio.

The application of this technique to conventional STOL aircraft has been severely restricted by the availability of suitable auxiliary power units. It is readily demonstrated that STOL aircraft require blowing air in quantities considerably in excess of that provided by conventional bleed rates from the main propeller engines.[11]

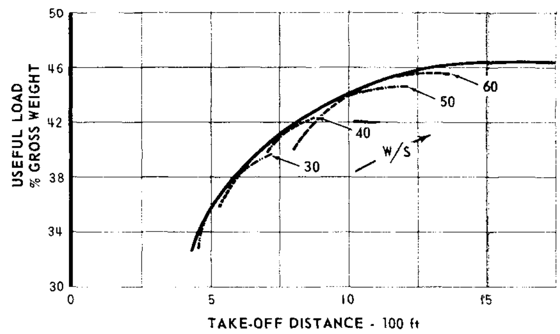


Fig. 1 Variation of useful load with takeoff distance.

2.1.3 Auxiliary thrust

A third possible method of improving STOL performance is by means of auxiliary light weight thrust engines mounted so as to provide both accelerating thrust along the flight path for takeoff and reverse thrust, in flight and on the ground, for braking in the landing maneuver. The application of these specially designed light weight engines to the VTOL regime has received considerable attention; STOL applications of the same power units have received considerably less. Using existing light weight engines, a thrust level of 0.10 w may be achieved with an installed structural penalty as low as 0.025 w. The use of this approach opens up the possibility of providing an additional responsive longitudinal aircraft control. As discussed below, it appears that significant additional operational improvements can be gained through the use of such a control.

2.2 Aerodynamic Features Relating to Performance

Certain aerodynamic features inherent in the approaches described previously limit the obtainable aircraft performance. Some of these features are discussed and compared in this section. Figures 2–4 show typical performance polars for the three improvements described and illustrate some of the significant features of each. Polars representative of the landing maneuver have been chosen, since this case usually emphasizes any indicated trends. No attempt has been made to show "optimum" polars in any particular case. Figure 2 shows the conventional deflected slipstream polar (approximately 40% of the wing span immersed in propeller slipstream) and the modification that ensues, with the addition of an auxiliary reverse thrust level of 0.20 w. The landing polar applicable to the case of full span slipstream coverage is shown in Fig. 3. In addition, the effects of compounding the benefits of reverse auxiliary thrust are also shown. Figure 4 shows the potential performance of boundary-layer control for a wing of similar planform and flap areas when compared with that shown in Fig. 2. Again the effects of auxiliary thrust are included. The

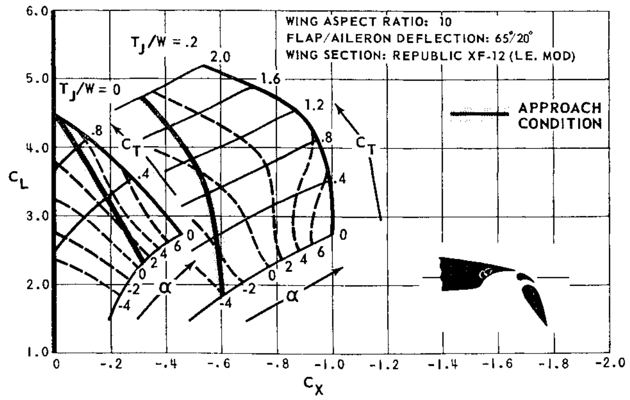


Fig. 2 Landing polar (conventional deflected slipstream configuration).

following aerodynamic features relating to aircraft performance are implicit to these polars.

2.2.1 Full span slipstream coverage

1) A considerably improved stalling incidence is achieved at high flap angles and propeller thrust coefficients relative to the conventional slipstream configuration. (This in turn produces an improved effective thrust deflection.) This feature results from the deletion of the highly loaded portion of the wing located immediately adjacent to the slipstream.

2) The wing-flap system may be deflected to a significantly larger flap angle at finite thrust coefficients without encountering stall.

3) Since the fully submerged concept relies on the achievement of very large, effective slipstream deflections, careful attention must be given to the proper matching of the wing aspect ratio and the propeller diameter/flap-chord ratio, since these factors will also significantly influence the effective thrust deflection achieved.

4) No excessive leading-edge loadings are encountered necessitating special leading-edge devices.

5) The substantial boundary-layer control effect, which may be obtained with large (slotted) flaps outside the slipstream proper, is forfeited (exchanged for the improved stalling incidence).

2.2.2 Boundary-layer control

1) Maximum lift will most certainly be limited by the leading-edge loading unless special devices are provided. The best solution, especially with thin wings (less than 10% t/c), will probably involve boundary-layer control at the leading edge as well as at the flap knee.

2) An apparent reduction in profile drag coefficient is realized at the higher lift coefficients, relative to the unblown flap configuration. This is attributable to the combination of a general improvement in the flap pressure distribution at a given lift coefficient and the direct recovery of a portion of the boundary-layer control thrust. This effect has an important beneficial effect on the inherent takeoff potential of this scheme.

3) The blowing thrust level that must be provided, per foot of span and chord, in the slipstream area to achieve attached flap flow at a given flap deflection is greater than that required in freestream. This increase is, however, considerably less than the ratio of total pressures, so that the apparent critical blowing coefficient is reduced by the slipstream. This effect is a result of the modified flap pressure distribution generated by the finite slipstream jet. The existence of this important effect is apparent in early NASA work[1]; however, quantitative documentation was first reported much later by Williams and Butler[2] of the Royal Aircraft Establishment. On the basis of this latter work and some recent model test work conducted by de Havilland, it appears that the following relationship is valid for high-aspect-ratio wings with conventional propeller diameter/flap-chord ratios, at least at small values of T_C (see Fig. 5):

$$\{C_{\mu_{ass}} = C_{\mu_{a_{freestream}}} \cdot [1 - (T_C/2)]\} \qquad (T_C < 1)$$

4) Significant economies can be realized in the blowing thrust required (or conversely an improved lifting effectiveness could be achieved with a given blowing thrust) if the spanwise distribution of blowing thrust is even crudely tailored to the spanwise variation in attachment requirements.

5) The critical blowing thrust coefficient, as conventionally defined, is a relatively poor criterion of excellence for a given flap geometry. (Experimentally, it is frequently observed that when C_μ is large, for a given flap angle, the corresponding ΔC_L is also large.) This coefficient depends on both the downstream adverse pressure gradient and the stability of the boundary layer ahead of the nozzle position. It is this latter condition which introduces the leading-edge and angle-of-attack dependence and explains, in part at least, the considerable difficulty associated with the correlation of the results of apparently similar model test work.

2.2.3 Auxiliary thrust

1) The modification of the three basic performance polars associated with the addition of a drag or reverse thrust force equal to 20% of the aircraft weight as shown in Figs. 2–4 is accomplished by the simple shifting of $\Delta C_X = -0.20C_L$ (no normal force interaction).

2) Significant normal force and pitching moment interactions can be encountered, depending on the relative location of

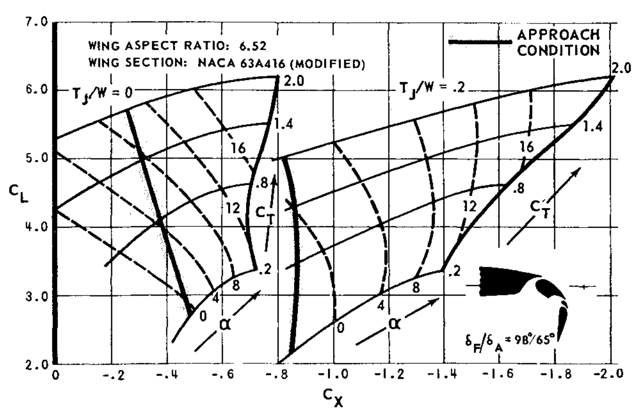


Fig. 3 Landing polar (deflected slipstream configuration with full span coverage).

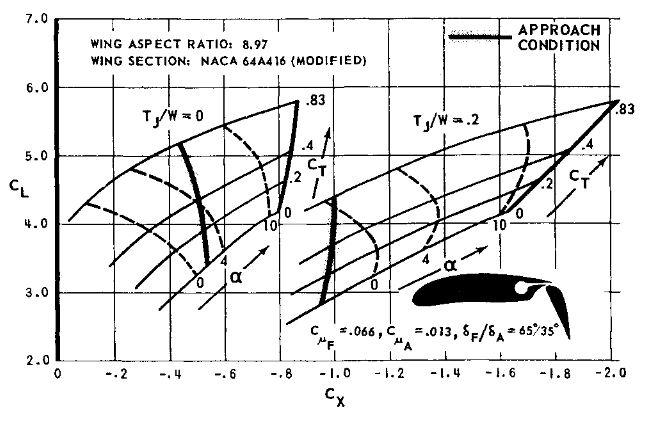


Fig. 4 Landing polar (deflected slipstream configuration with boundary-layer control).

wing and reverse jet flows. De Havilland jet model studies have indicated that if the horizontal reverse jet flow is constrained to a position below the wing chord plane, with maximum forward penetration somewhat aft of the wing leading-edge, a maximum normal force coefficient augmentation will be obtained. For these conditions lift augmentation of 20% has been measured with a wind-tunnel model incorporating a fuselage-mounted reverse jet with a thrust level also equal to 20% of the aircraft weight (see Fig. 6).

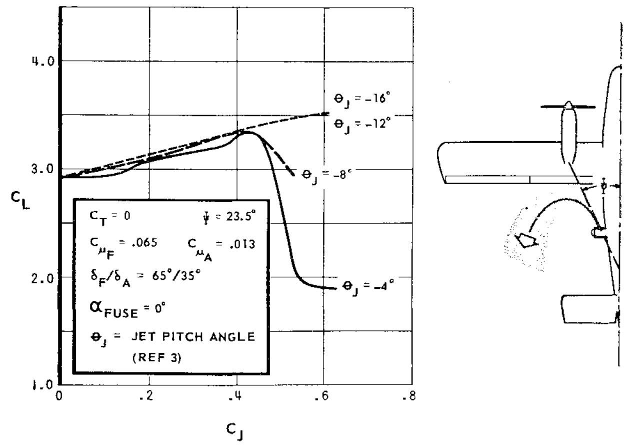


Fig. 6 Lift-reverse jet interference.

2.3 Comparison of STOL Performance Potential

In view of the many closely interacting limitations present in the STOL vehicle design, as indicated previously, no general statement concerning the likely performance potential of the foregoing schemes can be made. Nevertheless, certain particular features bearing directly on the inherent STOL performance potential of the schemes just described may be discussed in isolation.

2.3.1 *STOL takeoff potential*

Since the takeoff capability of the aircraft is intimately related to a number of design parameters, no simple general statement can be made concerning the potential of the preceding STOL improvements for this critical maneuver. The following general observation, however, can be made: for a given accelerating thrust-to-weight ratio available at the aircraft unstick speed and a fixed effective wing aspect ratio, there is a maximum lift coefficient that may be employed. This relationship is shown in Fig. 7, where the useful takeoff lift coefficient increases rapidly with T/W. (The level of the curve shown is slightly dependent on the assumptions made concerning the accelerations and speed margins permissible during transition; the shape is independent.)

Well-balanced STOL aircraft designs (with the thrust level dictated by the takeoff field requirements) will be located just below the indicated boundary. In connection with the improvements just discussed, the following general points may be noted:

1) To exploit the potential of wing boundary-layer control at moderate wing aspect ratios, a rather high level of thrust must be installed.

2) Auxiliary thrust will always improve the takeoff potential, but the rate of improvement may be reduced if the aircraft becomes severely lift-limited.

3) Designs incorporating full span slipstream coverage may achieve a good balance between lift and thrust by suitable variation of the slipstream deflection angle.

$$C_{\mu_a} = \frac{M V_J}{q_0 S}$$

$$C'_{\mu_a} = \frac{C_{\mu_a}}{q_{disc}/q_0}\left\{\left(\frac{D}{b_f}\right)_{eff} + \frac{q_{disc}}{q_0}\left[1-\left(\frac{D}{b_f}\right)_{eff}\right]\right\}$$

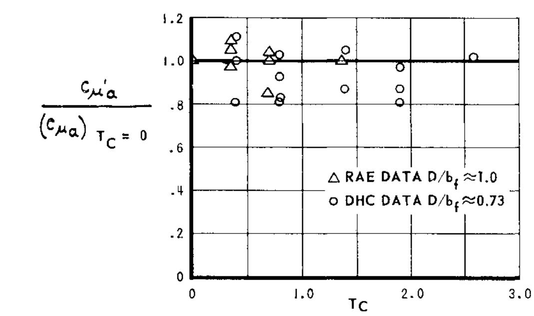


Fig. 5 Variation of boundary-layer control thrust with propeller thrust (D/b_f is the ratio of propeller diameter to flap span).

2.3.2 *Structural efficiency*

For the conventional propeller-driven STOL aircraft, an optimum combination of wing loading and installed thrust-to-weight ratio can be found which will maximize the disposable load of the aircraft at a given takeoff field size. The optimum wing loadings are found to increase as the required field size increases. If the wing lift is augmented by means of blowing boundary-layer control as described previously, a new set of optimum wing loadings may be found which is somewhat higher (15%) than the original. It is also found that the optimum values of the disposable loads at a given field size are comparable with the two systems. Thus it follows that, if a STOL design is forced by other considerations to operate at very high values of wing loading (in excess of optimum), a strong case can be made for the inclusion of wing boundary-layer control. (This design case may include a number of high-speed aircraft.) The inherent structural efficiency-achieved with the auxiliary thrust configurations appears to be quite good because of the very low specific weights of the available power units. In addition, some significant relaxation in the requirements of the main propulsive units can be obtained. It should be noted, however, that the installation weights of the light thrust units can be important.[4]

2.3.3 *STOL landing potential*

The landing performance that may be realized with the three STOL improvements described in the foregoing has been explored briefly. The landing performance is dominated by the aircraft wing loading and available maximum lift coefficients. (The usable propeller power levels are in all cases a small percentage of the design value.) Since it is of interest to explore a range of reverse thrust levels, variable auxiliary

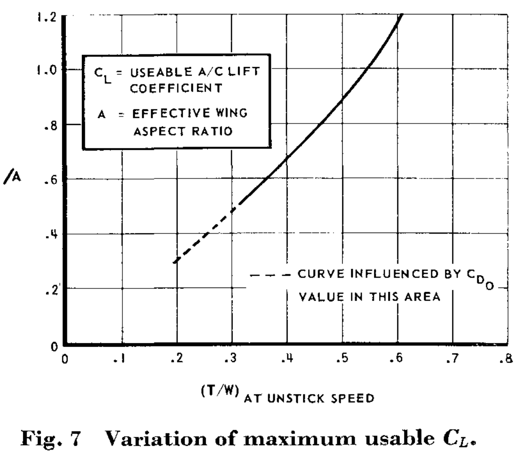


Fig. 7 Variation of maximum usable C_L.

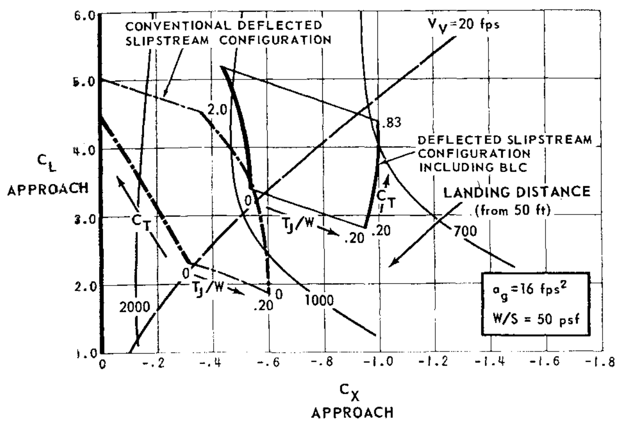


Fig. 8 STOL landing polars.

thrust has been considered with each of the following basic configurations: 1) a conventional deflected slipstream configuration (approximately 40% of the wing span immersed in the propeller slipstream); 2) a configuration with full span slipstream coverage; and 3) configuration 1 augmented with flap blowing. The overlay curves drawn on Figs. 8 and 9 indicate the landing performance that is achievable with a given approach speed (C_{LA} and wing loading 50 psf) and a given approach steepness (C_{XA}/C_{LA}). Limiting steepness-speed boundaries are drawn for each STOL configuration, the boundary positions being dependent on the level of propeller thrust and auxiliary thrust used. (These limiting approach lines are also drawn on the polar plots of Figs. 2–4.) The curves shown are applicable only to fully flared landing maneuvers at a wing loading of 50 psf with an average ground deceleration of 0.5 *g*.

It is important to note that the landing distance data shown directly reflect the characteristics of the basic aerodynamic polars of Figs. 2–4. As no attempt has been made to optimize these polars as discussed previously, these distance data indicated the likely trends for "plausible" rather than optimum wing-propeller configurations. The following trends are noted:

1) For each reverse thrust level, there is an optimum propeller thrust that minimizes the total distance. [This value is close to zero (or even negative) without auxiliary thrust.]

2) The optimum landings all require power settings such that an approach vertical descent velocity just less than 20 fps is achieved. This is quite an acceptable value.

3) The addition of auxiliary thrust allows a substantial increase in the usable propeller thrust and the accompanying maximum wing lift coefficient.

Figure 10 is a plot of the optimum distances obtained from Figs. 8 and 9 and is thus applicable only to fully flared landings at a 50-psf wing loading. It is seen that auxiliary thrust benefits compound similarly in all cases.

Figure 11 is a plot of the optimum landings (as indicated previously) for the conventional deflected slipstream configuration only (polar data given in Fig. 2), exhibiting the trends due to wing loading. Similar trends are obtained for both of the other configurations.

3. Revolutionary STOL Improvements

The recent development of high-pressure ratio, high-bypass ratio, or fan-type jet engines presents, for the first time, the possibility of plausible jet-powered competition for the traditional propeller-driven STOL aircraft. These engines are capable of low specific weights and may be fitted with lightweight thrust-vectoring nozzles.

Several approaches have been pursued in an attempt to restore the low-velocity lift and thrust level achievable with the propeller. The external and internal jet-flap schemes[5,6] were perhaps the earliest attempts. The Hawker-Siddeley Aviation Group have pioneered an alternative approach that involves the deflection of the propulsive jets for low-speed lift augmentation combined with an improved basic wing lift obtained through the use of blowing boundary-layer control.

3.1 Deflected Jet Configuration

The most difficult area in which the deflected jet configuration must compete with the slipstream aircraft occurs in the takeoff maneuver. The deflected slipstream achieves its efficient takeoff potential through the combined action of slipstream vectoring and flap boundary-layer control action outside of the slipstream. The competitive position of these two aircraft in the airborne phase of the takeoff maneuver can be seen in the performance polars illustrated in Fig. 12. In this figure the lift and effective drag at 0.9 of maximum lift have been plotted for a range of flap angles and thrust coefficients, for both a double-slotted flap slipstream configuration (40% span immersion) and a simple flapped wing employing boundary-layer control and thrust vectoring (without aerodynamic interaction). Again no attempt to show optimum aerodynamic data has been made for either configuration. However, the curves do represent comparable and probably typical cases (as to wing aspect ratio and flap extent). The following comparisons of the takeoff capability of two configurations can be made:

1) The lift and thrust levels at a given coefficient are comparable over the complete range of flat angles. The airborne performance achievable with these configurations will, therefore, be similar at equal values of unstick thrust-to-weight ratio and wing loading.

2) The jet configuration incurs a small performance penalty at the high-lift coefficients shown, because attach-

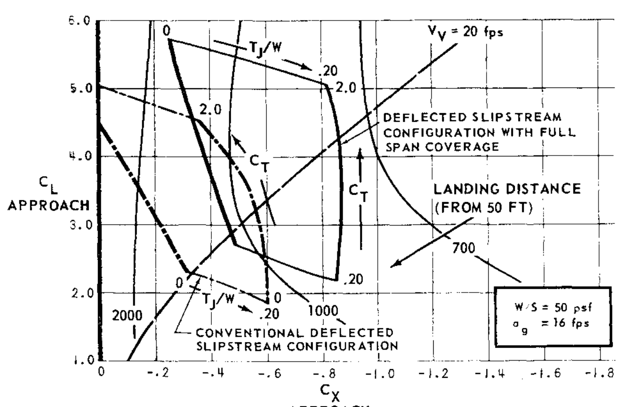


Fig. 9 STOL landing polars.

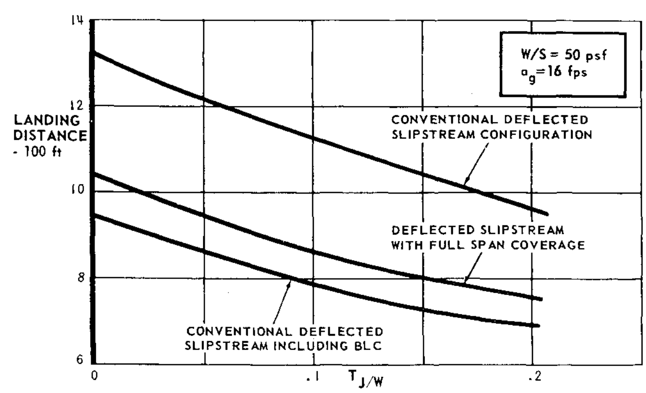


Fig. 10 Effect of auxiliary reverse thrust on STOL landing distance.

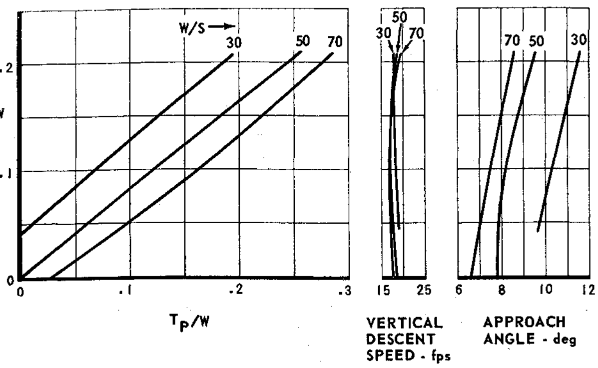


Fig. 11 Characteristics of optimum landings (conventional deflected slipstream configuration).

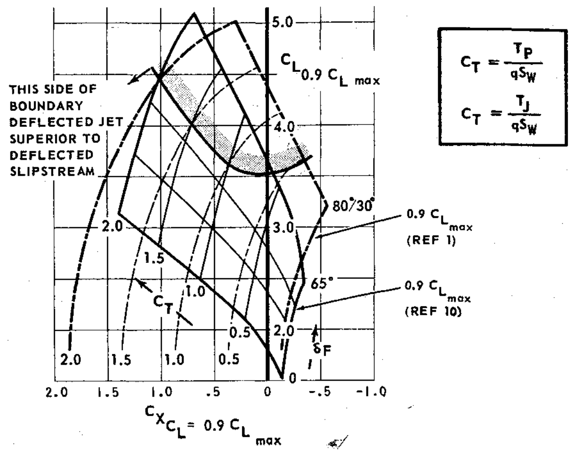


Fig. 12 Comparison of deflected slipstream with deflected jet and boundary-layer control in takeoff transition.

ment-blowing boundary-layer control thrusts have been used at all flap angles for the deflected jet polars. It appears that some degree of overblowing would improve the competitive position of the jet deflection configuration at large flap angles.

3) The deflected thrust configuration will operate at higher effective flap angles for equal performance.

4) During the takeoff ground roll, the deflected jet aircraft can operate with a significant acceleration advantage because of the provision of rapid thrust-vectoring independent of flap angle.

5) The use of boundary-layer control with the jet aircraft appears mandatory if a competitive position is to be achieved with the slipstream configuration.

4. Controllability at Low Forward Speeds

The provision of extreme levels of STOL performance in a given aircraft design is of little significance if this performance level is only achieved by a skilled and practiced test pilot, on a relatively low percentage of occasions under carefully chosen atmospheric conditions. Both pilot and aircraft control limitations must be considered at the outset if a vehicle is to evolve which will allow the average pilot under average atmospheric conditions to achieve a high percentage of the maximum brochure performance.

4.1 Longitudinal Control during the Approach

The limitations of the man-machine control system conventionally found in STOL aircraft are emphasized by the rather severe requirements of the landing maneuver. During this maneuver the required number of critical judgments and tasks which must be effected in a very short time duration is large. In addition, the amount of quantitative information normally available to the pilot is quite limited. Consequently, the repeatability of the critical aspects of this maneuver is considerably reduced.

4.1.1 Conventional longitudinal control

Two longitudinal controls are normally provided, i.e., elevator or stick control and power or throttle control. During the landing approach with full flap, the conventional STOL aircraft is very sensitive to propeller-power adjustments. Thus the throttle control simultaneously affects many of the aircraft variables that the pilot is attempting to control independently, making precise control difficult. If the aircraft remains temporarily at a fixed wing incidence, the following items will respond immediately to a throttle command: longitudinal trim, wing lift, stalling incidence margin, and aircraft longitudinal acceleration. Elevator control, on the other hand, affects the stall margin and aircraft lift immediately and ultimately affects the aircraft velocity and rate of descent (throttle constant). It is observed that these controls have objectionable interactions that do not simplify the piloting task. In addition, with conventional throttle control on STOL aircraft, the optimum trim power condition during the landing approach will be at a very low or idle power setting, which implies that the transient power response is considerably compromised and that glide path control in one direction only (shallower) is possible. It has been observed that, under gusty air conditions, the pilot is unable to perform repeatedly with this severe control restriction. In these cases an increased power level must be employed by the pilot on the approach, which reflects a serious performance penalty.

4.1.2 Modified longitudinal control

In an attempt to remove some of the deficiencies observed with conventional control and to reduce the scatter associated with high-performance landings, a modified longitudinal control system has been evaluated at de Havilland. In this same connection, it was also desired to provide the pilot with a means for controlling the float phase of the landing maneuver. (It has been observed that this latter feature contributes very significantly to landing distance scatter achieved with conventional control.)

The scheme that was evaluated[7] employs modulation of an auxiliary thrust engine mounted in the rear fuselage. This scheme replaced the conventional throttle control with a fast-acting thrust-drag control while retaining the elevator control. The thrust nozzles were carefully located so as to achieve essentially zero trim interference over the complete thrust-drag range. A variable-geometry nozzle was employed so that constant rpm operation could be used to provide a very responsive control. The thrust installation had a total range of approximately -0.20 to $+0.20$ w with a time for full travel of approximately 2 sec. The propeller power setting was normally maintained at a constant preselected value during these evaluations and thus was aerodynamically equivalent to an increased flap angle. A wide range of approach angles and speeds was evaluated during the test flying with this experimental configuration (Fig. 13), and a limited amount of statistical data was accumulated to indicate the potential of the modified control used. Some of the results are shown in Figs. 14 and 15.

The first of these shows the distribution of the actual landing distances achieved by a skilled pilot employing the modified control. For reference, the corresponding data for two other de Havilland STOL aircraft, each flown by a skilled pilot, are included. It is seen that the percentage deviations of the landing distance from 50 ft have been improved by the use of the modified control. This gain is even more significant when it is noted that the experimental aircraft with

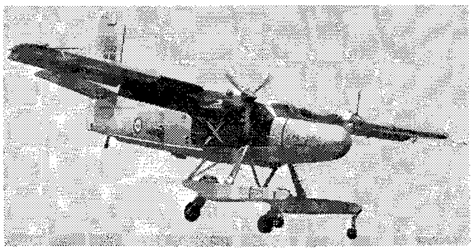


Fig. 13 Experimental aircraft incorporating modified longitudinal control.

the modified control had an absolute landing distance approximately one-half that of the aircraft with conventional control. One would, therefore, expect that any distance deviations resulting from fixed pilot reaction or decision times would be of greater importance for the experimental aircraft.

Figure 15 indicates the results obtained when the pilot was constrained to touch down at a fixed point on the runway. In addition, the importance of providing a rough glide-slope indicator has been examined, and the results achieved with a simple slope marker are included. (Two markers, of distinctive shape and color, were mounted beside the runway. When viewed from the correct trajectory angle, these markers were "in line.") By comparing these two figures it is noted that, with the modified longitudinal control, a large portion of the total distance variation is apparently associated with pilot errors that are directly traceable to the provision of poor or inadequate aiming information.

4.2 Speed Stability during Glide Approach

The existence of adequate aircraft speed stability during the final stages of the landing approach is unquestionably of considerable importance in providing acceptable longitudinal control to the STOL pilot. The existing criterion associated with this stability is very closely related to the criterion for flight on the "back side" of the drag polar, as defined by

$$[(C_D + C_{AS})/C_L] - (dC_D/dC_L) < 0$$

where C_D represents the total aircraft drag coefficient and C_{AS} represents the equivalent drag coefficient of the aircraft powerplant at constant throttle setting, i.e., $C_{AS} = -(1/S)(\partial T/\partial q)$. It is significant to note that C_{AS} is positive for propeller-driven aircraft, thus extending the C_L range over which conventional elevator response is obtained. The real significance of operation on the "back side" of the drag polar arises from the reversed elevator-height response that occurs. For aircraft with a positive static margin, this results in an ultimate height change of opposite sense to that accompanying the initial elevator response.

It has been suggested that the foregoing criterion can be used to establish a speed stability criterion for aircraft in the landing approach condition. In fact, Lean[8] has proposed the following quantitative dimensional criterion

$$-\frac{C_L}{W/S}\cdot\left(\frac{C_D}{C_L} - \frac{dC_D}{dC_L}\right) < \begin{matrix} -0.001 \text{ IFR} \\ +0.001 \text{ VFR} \end{matrix}$$

as establishing acceptable aircraft practice. A brief exploration of the background associated with this criterion has been carried out which strongly suggests an alternative approach to the establishment of a suitable stability criterion. The assumptions required to obtain the preceding criterion[9] completely suppress the moment equation (from the three longitudinal equation set) and replace it with an elevator law that can be alternatively interpreted as $\delta_e = f(\Delta\varphi)$ with infinite gain (see Appendix). It is therefore expedient to consider the location of the roots of a system consisting of the complete aircraft (longitudinal) together with an autopilot of

Table 1 Longitudinal stability coefficients

u	w	θ	δ
$2\mu_S + 2C_D$	$-C_L + C_{D\alpha}$	$+C_L$	0
$2C_L$	$2\mu_S + C_{L\alpha} + C_D$	$-2\mu_S + C_T - C_D$	0
0	$-C_{m\dot{\alpha}}s - C_{m\alpha}$	$i_B{}^2s^2 - C_{mq}s$	$C_{m\delta}$

$[N]_\varphi \equiv [N]_{\theta-w} = 2C_{m\delta}\,[\mu(C_{L\alpha} + C_T)\,s + C_D\,(C_{L\alpha}+C_T) - C_LC_{D\alpha}]$

the type $\delta_e = f(\Delta\varphi)$ having variable gain characteristics (see Fig. 16). The motion of the basic aircraft roots (S_p and L_p) associated with an increasing gain in the elevator autopilot control loop is sketched qualitatively. It is found that, for some finite gain in the elevator autopilot control, the modified-aircraft short-period mode will become unstable. Simultaneously, the long-period mode decreases in frequency, and, eventually, at high gain it generates into two simple subsidences, one of which moves toward the origin (zero damping). It can be shown (Appendix) that the limiting position of the degenerate phugoid subsidence, on the zero-frequency axis, is given by the criterion suggested originally by Neumark.[9]

The actual gain level that the pilot will introduce to control the aircraft on the approach is difficult to estimate. Assuming that the pilot prefers a pure gain-type control (between flight trajectory angle and elevator), it is probable that he will introduce increasing gain up to the point where no further significant phugoid damping is achievable and where the short-period damping is approaching a minimum desired level (point 1, say). (The pilot is effectively exchanging damping from the short-period mode to the long-period mode.) The location of the final closed-loop gain is very probably considerably short of the position A given by the preceding Neumark[9] criterion. This criterion represents, therefore, a considerable simplification of the true closed-loop aircraft longitudinal stability characteristics (evaluating only the degenerate phugoid subsidence damping) that would be encountered at very high gain with a simple elevator control.

In conclusion, it is felt that the previous stability criterion requires some amplification because of the following:

1) The implied infinite control gain is not possible or even desirable with the human operator.

2) Neglecting the foregoing point (for suitable autopilot control), the difference between a stable and unstable long-period subsidence or divergence will not be critically appreciated by the pilot (i.e., a speed error halving in 1 min is not worse than a speed error doubling in the same time).

3) Of all of the variables the pilot can monitor, the glide approach angle is probably the least well documented, at least in current aircraft.

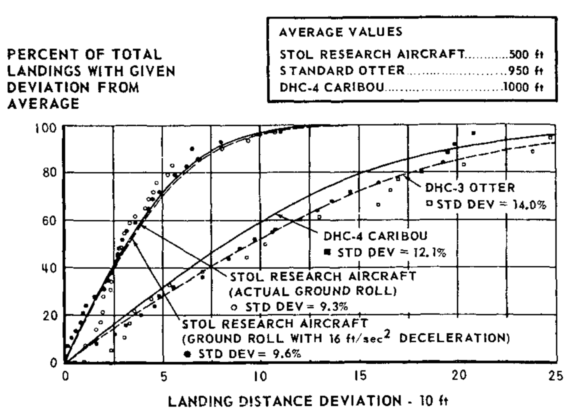


Fig. 14 Cumulative statistical distribution of STOL landing distances (from 50-ft altitude).

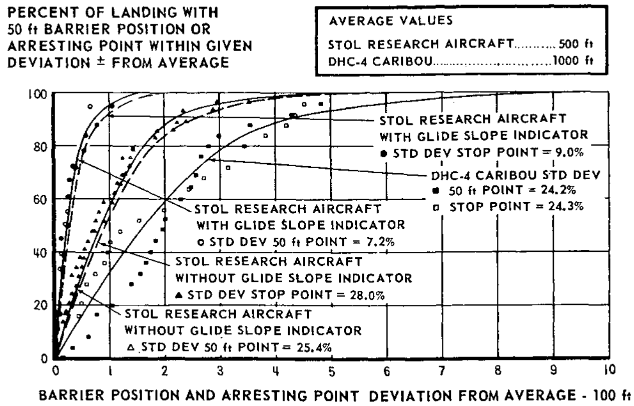


Fig. 15 Cumulative statistical distribution of the horizontal location of STOL landings.

5. Appendix: Identification of Neumark's Stability Criterion on the Root Locus Plot

If a simple closed-loop longitudinal control is considered (between elevator angle and aircraft glide path angle) which incorporates pure gain feedback, Neumark's[9] stability criterion is readily identified as the limiting position of phugoid root, when the feedback gain is infinite. This limiting position occurs on the real axis with an intercept value given numerically by the original Neumark stability criterion (Fig. 16). The following abbreviated development of this limit is given.

By using the conventional longitudinal, small-perturbation stability equations (and notation) with independent (perturbation) variables u, W, θ, and control variable δ, the numerator determinate $[N]_\varphi$ for solution in $\varphi \equiv \theta - \alpha \equiv \theta - W$ can be developed (see Table 1). The transfer function between φ and δ is developed in terms of numerator $[N]_\varphi$ and denominator $[D]_\varphi$ determinates as shown in Fig. 17.

The roots being obtained with the complete denominator term equal to zero, the following cases are noted: 1) when $K = 0$ roots given by $[D]_\varphi = 0$ (poles), and 2) when $K = \infty$ roots given by $[N]_\varphi = 0$ (zeros). Expanding this latter condition ($[N]_\varphi = 0$),

$$s = -\{C_D - C_L[C_{D\alpha}/(C_{L\alpha} + C_T)]\}/\mu$$

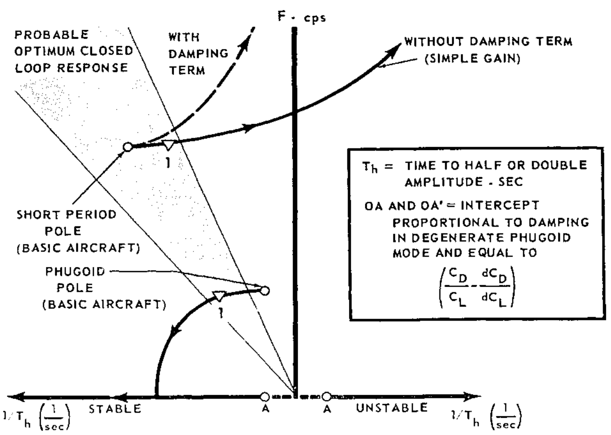
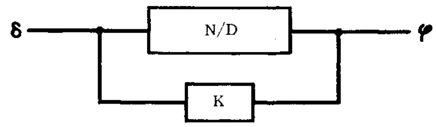


Fig. 16 Root locus plot of STOL aircraft with glide path auto pilot of form $\delta_e = f(\Delta\varphi)$.



$$\varphi/\delta = \frac{N/D}{1 + K\,N/D} = \frac{N/K}{D/K + N}$$

Fig. 17 Transfer function.

thus the limit of the real root for $K \to \infty$ is stable if

$$C_D - C_L[C_{D\alpha}/(C_{L\alpha} + C_T)] > 0$$

and in level flight with $C_T = C_D$, this condition reduces to

$$C_D - \frac{C_L(dC_D)}{1 + (C_D C_{L\alpha})} > 0$$

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