V/STOL Aerodynamic Testing Techniques at British Aircraft Corporation

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Powered-lift aircraft aerodynamic testing at the Military Aircraft Division of British Aircraft Corporation is focused on 5.5 m (18 ft) closed working section, nonreturn wind tunnel. The facility operates over a narrow freestream speed range of approximately 11-21 m/s (35-70 ft/s). Full-scale Mach number is not maintained, therefore, in the interests of drive system cost. The wide range of propulsion force coefficients required are simulated by changing the propulsion momentum while keeping the freestream momentum constant. Where possible, the propulsive forces are grounded (nonmetric), and the airframe loading due to propulsion and freestream interaction is measured with high resolution using internal strain-gage balances tailored to the expected load range. Propulsive stream flows are represented by cold compressed air taken onboard through the sting support system and used either as direct feed to jet flows or as the drive air to compressed air ejectors, for better representation of airbreathing propulsive systems. While none of these techniques are novel, together they constitute a cost-effective testing capability, ideally suited to "proof of concept," preliminary project design, and basic research testing on power-lift aerodynamics.

Nomenclature

A_j = lifting jet nozzle exit area
b = aircraft wingspan
C_0 = aircraft wing root chord
$\bar{C}$ = aircraft wing geometric mean chord
C_L = airframe lift coefficient
C_m = airframe pitching moment coefficient
C_s = suction coefficient $\int p_s/q_0 \, dA$
C_μ = thrust coefficient $T/q_0 S$
C'_μ = component of thrust coefficient normal to freestream
d_j = lifting jet nozzle diameter
h = height of aircraft above ground
L = airframe lift force
M = airframe pitching moment
M_0 = freestream (or tunnel) Mach number
p_s = local surface static pressure
q_0 = freestream (or tunnel) dynamic pressure
R = Reynolds number
r_j = lifting jet nozzle radius
S = aircraft wing reference area
T = lifting jet nozzle thrust
V_0 = freestream (or tunnel) velocity
V_j = lifting jet nozzle velocity
V_E = equivalent velocity ratio = $V_0/(\rho_j/\rho_0)^{1/2} V_j$
x_j = position of lifting jet along the root chord from the leading edge
α = angle of incidence
δ_j = wing flap deflection angle from the chord line
δ_N = lifting jet nozzle vector angle from the aircraft horizontal datum
ρ_0 = density of the freestream (or tunnel)
ρ_j = density of the lifting jet nozzle mass flow
μ_0 = kinematic viscosity of the freestream (or tunnel)

Introduction

IN the aerodynamic development of a new design, wind-tunnel testing usually features prominently at an early stage. This is particularly true of powered-lift concepts where confidence in prediction is, in general, lower than for more conventional designs. Tests are required for "proof of concept," provision of design data, or to highlight problem areas which may need more detailed study or simulation or even changes in design concept.

Powered-lift testing is potentially expensive by virtue of the need for a large working section and a fairly detailed representation of the propulsive flows. The elapsed time to build a model can be long, and it follows that the simpler the operation can be kept the more test work can be obtained for a given budget and the quicker the reaction will be in getting a representative model into the tunnel.

The objective is to keep it simple and still remain effective. What follows is a description of our techniques and facilities for this class of work in rather more detail than has been previously presented,[1] together with an appraisal of their cost-effectiveness.

Test Facility

The hierarchy of test requirements should be headed by the need for a large working section. Our facility, shown in Fig. 1, measures 5.5 m (18 ft) wide and 5.0 m(16.5 ft) high. Small working sections produce either significant wall constraints or unmanageably small models. Before our facility was built in 1963 we used very small models in conventional tunnels and found the scope for geometry representation and propulsion simulation severely limited. At that time, and still today, wall constraint prediction and correction was and is a risky business in conditions of high lift and spanwise nonuniform lift distributions.[2]

The installed power in this tunnel is very low, a mere 200 kW (250 hp), which gives a maximum working section speed of 21 m/s (70 ft/s). This low power level was chosen to keep the first costs of the tunnel down; it being argued that simulation of full-scale Mach number was not a high-priority consideration and, with drive power a function of tunnel speed cubed, not cost-effective. A nonreturn circuit layout was chosen on similar arguments, reinforced by the idea that it would not be advisable to recirculate highly disturbed wakes without a great deal of flow treatment, again leading up the cost spiral.

Other features of the facility worth mentioning include: 1) a sting support system for internal strain-gage, balance-mounted models shown in Fig. 2 (the tunnel is not equipped with a mechanical balance); 2) a compresssed air supply and control system, taken from our high-speed "blow down"

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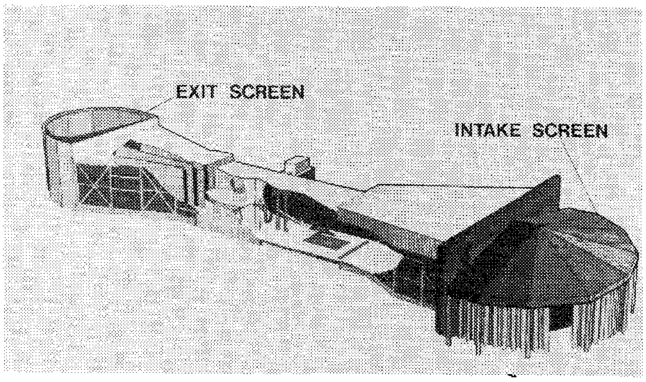


Fig. 1 V/STOL tunnel.

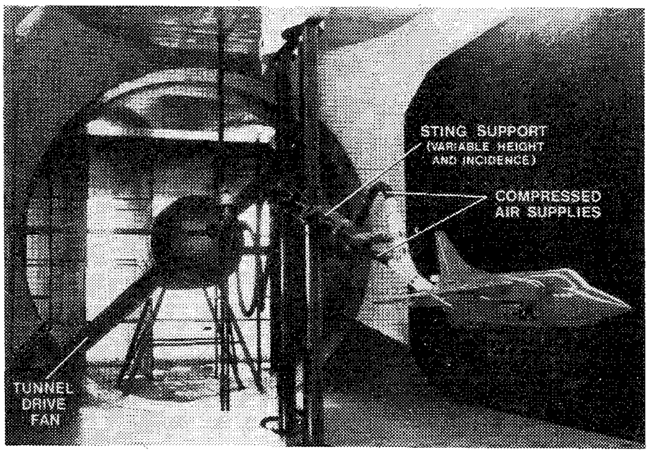


Fig. 2 Sting support system.

facility, capable of delivering 4 kg/s (9 lb/s) continuously at up to 40 atm, and about twice this mass flow for short periods; 3) continuous moving belt built into the floor with a full-span boundary-layer suction slot upstream; 4) a spacious laboratory for model preparation including a two-component thrust balance; and 5) a minicomputer-controlled on-line data acquisiton system.

Consequences of the Nonreturn Circuit Choice

Before discussing the main features of the test techniques, a brief outline of the consequences of the nonreturn circuit decision and what we have recently done about it, is worth including. The nonreturn choice was made largely on cost arguments, together with the removal of the model wake, but there are disadvantages—mainly, gust sensitivity. Until 1975, the inlet and exhaust were not protected from the external wind. The inlet faces west into the prevailing wind which is often strong, 20 knots is common, and contains large-scale low-frequency gusts. Changes of direction giving rise to cross-flow velocity ratios approaching unity are also common.

Based on wind statistics and model tests, it was estimated that wind screens would raise the overall utilization from about 50% of the working days to at least 70%. In fact, since the screens were fitted in 1975, performance has been better, with no test days lost due to external winds. Wind screen geometry is shown on Fig. 3. The inlet design concept was taken from Arnold Engineering Development Center studies[3] and optimized by a 1/22 scale model test program to achieve a high degree of protection for a low power loss.

The spatial and temporal behavior of the flow in the working section was measured by the model with and without the external wind screens. These tests indicated that with the screens on, the working section is still sensitive to the external wind, but so much less than with the screens off that satisfactory test conditions should exist in most external wind states. At full scale, a full quantitative picture of the behavior with screens has not yet been obtained, but some data on the temporal behavior does confirm the model test amplitudes measured and, qualitatively, the model tests seem to be valid in that good quality data are now being obtained in all external wind states met so far. In this context, it must be said, however, that signal filtering and integration, typically over 3 seconds, are also contributory to reduced data scatter.

Model tests indicated that the power loss from the screens would produce a 5-10% loss in tunnel speed, relative to the unscreened tunnel in still air. In practice, the design speed of 20 m/s is easily obtained with the screens fitted, but the simultaneous replacement of the time-expired drive motor by one of a similar power rating may be a contributory factor.

At 1975 prices, the first cost of the total facility was very low at $300 K ($100 K in 1963), including instrumentation, and resulting from the low-power and open-return choices. An additional $200 K has been spent in 1975 on the wind screens (about $100 K), replacement drive motor, and modern data acquisition system. The facility, as it now stands, including all minor developments, at 1977 prices would cost less than $1 M to replace.

The Case for Reduced Velocity Scale

Powered lift testing is often thought of in terms of operating the wind tunnel over the full-scale speed range of V/STOL flight (i.e., 0-150 m/s). There is no reason why this should be other than to achieve full-scale Mach number and a Reynolds number deficiency dependent on geometrical scale only. Since the law of diminishing returns applies strongly to the cost-effectiveness as tunnel speed is increased (drive power $=f\ V_0^3$), it is well worth questioning the need for Mach number simulation. In our particular circumstances in 1963, it was quite simply a case of reduced velocity scaling or no facility at all. Since that time, Reynolds number and Mach number effects have been studied whenever possible, and caution has to be exercised in cases where airframe flow separations may influence the measurements. Table 1 shows

Table 1 Reduced velocity scale

$V_E=V_0/V_j$	0.05	0.075	0.10	0.15	0.20	0.30	0.50
$C_\mu\ (S/A_j=40)$	20	9	5	2	1.25	0.5	0.2
V_0 full scale, m/s	20	30	40	60	80	120	200
V_j full scale, m/s	400						
V_0 model scale, m/s	20						
V_j model scale, m/s	400	267	200	133	100	67	40
$\frac{V_0 \text{ model scale}}{V_0 \text{ full scale}}$	1.0	0.67	0.5	0.33	0.25	0.17	0.1

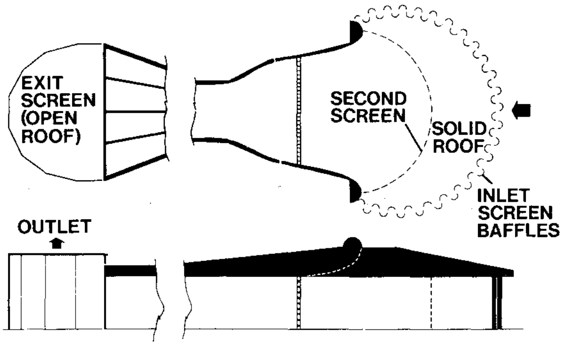


Fig. 3 Inlet and exit screen geometry.

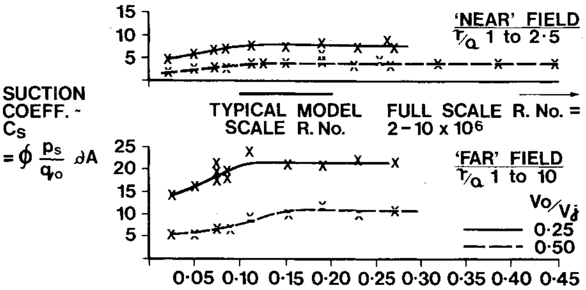


Fig. 4 Jet in crossflow—Mach number sensitivity.

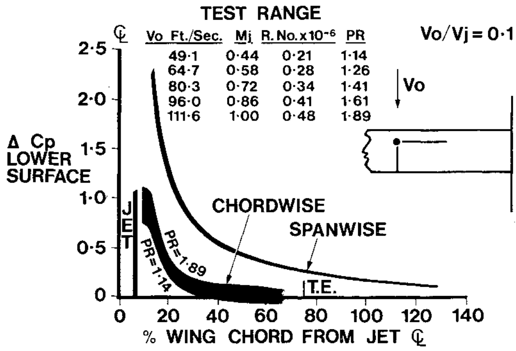


Fig. 5 Jet in crossflow—Mach number similarity.

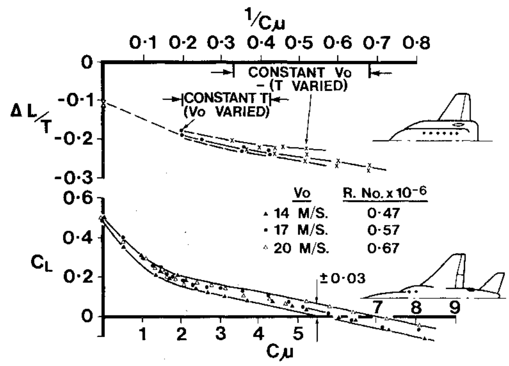


Fig. 6 Effect of changing jet and freestream velocity.

typical values of the test velocities compared to full scale.

It is now widely accepted that the nondimensional propulsive flow coefficients are the first priority for simulation. These coefficients include both propulsive and airframe reference areas (geometric scale) and propulsive to freestream momentum ratio (flowfield similarity). Flowfield similarity can, therefore, be satisfied without resort to full-scale velocities. Nevertheless, the simulation deficiencies of the Reynolds numbers and Mach number need to be studied to try to ascertain the probable risk involved in their non-representation.

Some results from a classic jet lift experiment are shown on Fig. 4. These tests were carried out to answer the suggestion that jet lift at forward speed would be Reynolds number sensitive by the analogy of flow around a solid circular cylinder. A jet exhausting normal to the freestream from a quasi-infinite plane wall gives rise to a pressure distribution on the wall. The pressure integrations show no Reynolds number sensitivity over the range tested. The range covers all model tests in the tunnel, but does not extend to full scale. The tests, therefore, constitute an indication and not proof. Today it is much more widely understood that the jet under crossflow is significantly different from the flow around a solid cylinder particularly with regard to the downstream wake behavior and Reynolds number sensitivity.

The technique also neglects the effect of Mach number and hence jet pressure ratio. Apart from the enormous reductions in drive power and hence the cost possible by so doing, good airframe loads resolution using internal strain-gage balances is achieved due to the narrow test speed range and compressed air consumption is reduced as the jet pressure ratio falls with reducing thrust coefficient. Some justification for this is indicated by data from an experiment[4] designed to address the question of jet simulation parameters for lifting jets at forward speed. Figure 5 shows that only downstream of the jet exit is there any significant spread of pressure coefficients at the same equivalent velocity ratio, and this is random and due to unsteady flow conditions rather than to any consistent function of pressure ratio. Similar results are found with regard to jet temperature. The conclusion that V_E is the dominant variable, and not the values of velocity and temperature used must, however, be qualified, because it might be expected that the "near field" would be influenced by the jet structure which may vary with pressure ratio, e.g., a pressure ratio above choking represented on a test by an unchoked jet. It appears that, in most cases, the near field does not dominate except perhaps in the hover, or zero forward speed, mode. In hover, the ability to operate at full-scale pressure ratio need only be limited by compressed air capacity.

Within the scope of the narrow speed range, the opportunity is taken, whenever possible, to run a test with a thrust coefficient overlap by different velocities. Some data taken from two test programs are shown on Fig. 6. There is no trend due to the velocity of simulation shown, merely the normal level of scatter to be expected.

This does not constitute proof, but certainly gives a strong indication that reduced velocity scale testing is acceptable. There will be conditions where this will not be good enough and technique research should continue to be aimed at defining the conditions and extent of possible error. In the U.K., some fundamental work along these lines is continuing.

Grounding the Propulsion Forces

Since the propulsive force can be large compared with aerodynamic forces, accurate definition of the latter and the interference effects may depend on its separate measurement. A linearized approach is adopted, whereby the propulsion forces are separately measured or precalibrated, the airframe forces are measured "power off" and "power on," and a linearized power interference term defined by difference. Depending upon the degree of integration of the propulsion system, it may become very complex to separate it from the airframe. Each case has to be studied in terms of cost, complexity, and resolution against the particular test objectives. Jet lift systems with no inlet flow simulation do not usually present any difficulties, but the arrangements become more complicated with airbreathing simulators (ejectors or fans) but are often still practical. It is not generally found to be practical in the cases of internal blown flaps and augmentor wings.

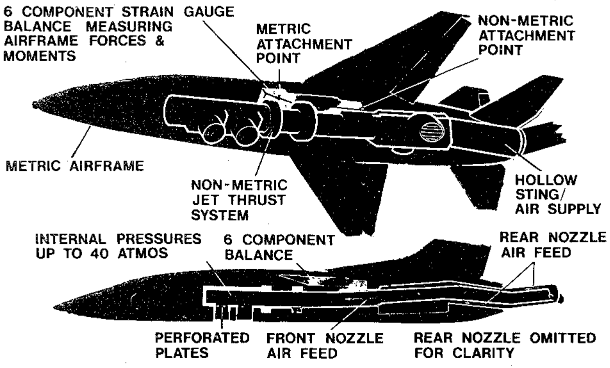


Fig. 7 Metric/nonmetric technique.

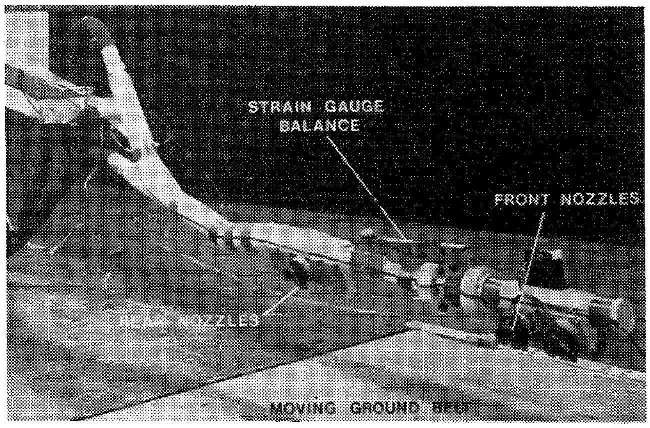


Fig. 8 Model internal arrangement.

A schematic arrangement for a "lift plus lift/cruise" propulsion system without inlet simulation is shown on Fig. 7. The sting support forms the airfeed to the lift and lift/cruise nozzles and the grounded end of a six-component airframe balance. The sting and nozzle assembly for a particular configuration is shown in the tunnel on Fig. 8. In the region of the nozzle, clearance between nonmetric nozzles and metric airframe has to be provided. The arrangements for two different types of lift nozzle are shown on Fig. 9. On the rotating cascade nozzle, the open door is taken as an airframe component. With the eyelid deflector type, the inner face of the deflector is taken as belonging to the propulsion system but the outer face as airframe, hence the deflector lid has to be shrouded. By taking care to seal the airframe everywhere else, it has not been found necessary to seal around the nozzle exits, thereby avoiding seal constraints. With the airbreathing simulator (Fig. 10), similar arrangements at the nozzles are made, but the inlet is a different and more difficult problem. Pre-entry thrust and intake-induced airframe interference terms are inseparably distributed on the airframe. Consequently, it is considered necessary to mount the inlet to the airframe and make the metric break downstream at the engine face. This break has to be sealed with no leakage. In the particular installation shown, the engine is balance-mounted, and in order to correct the engine balance and airframe balance for the seal stiffness and pressure tares, it is necessary to split the seal into two parts with the nonmetric plate between.

An example of the grounded propulsion technique resolution is given on Fig. 11. The lift from a jet lift test in ground effect at forward speed is presented in a number of ways. The test used the grounded propulsion technique and therefore the actual test data are plotted as airframe lift coefficient C_L vs jet thrust coefficient C_μ or as interference lift C_L/C_μ vs velocity ratio V_E. Also shown are the total lift against thrust coefficient for the thrust alone, thrust plus

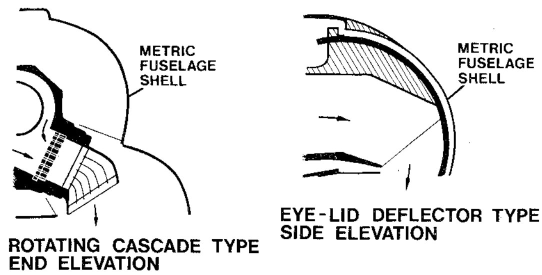


Fig. 9 Lift nozzle shrouds.

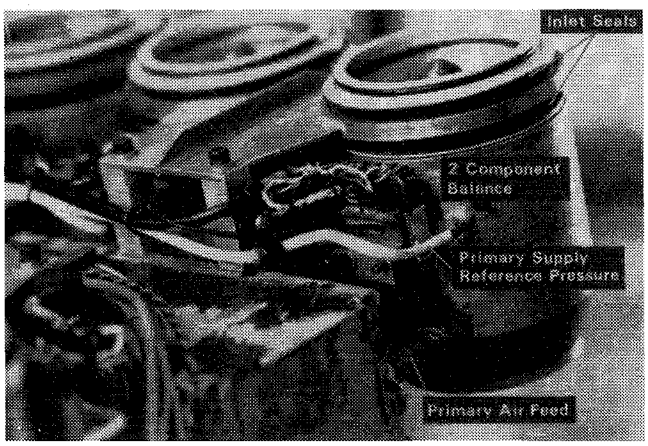


Fig. 10 Airbreathing simulator mounting.

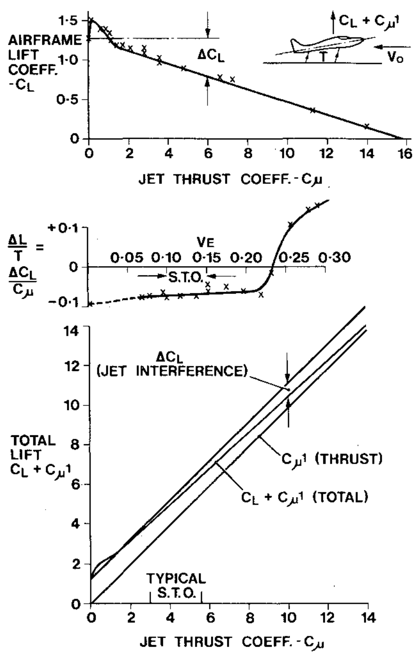


Fig. 11 Breakdown of total lift.

airframe power off (i.e., no lift interference), and the correct total lift (thrust plus power on airframe). Looking at the total lift curves, it is not difficult to see that in order to obtain the same resolution of the jet-induced term from a total force measurement would demand very high resolution of both the total and the thrust-alone terms. Scatter, due to thrust error, could easily obscure the characteristics of the induced lift. This has occurred many times in the past, leading to poor understanding and poor correlation of data from different sources. In particular, this has occurred with many fan tests,

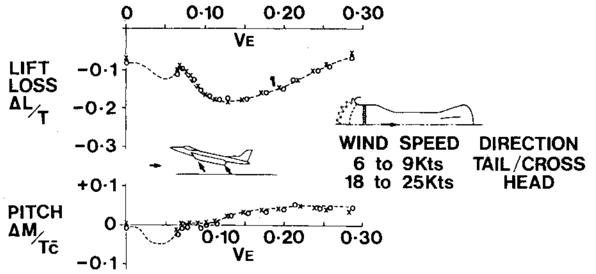


Fig. 12 Repeatability of data.

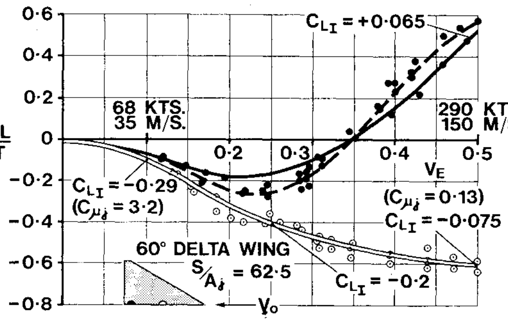


Fig. 13 Jet chordwise location.

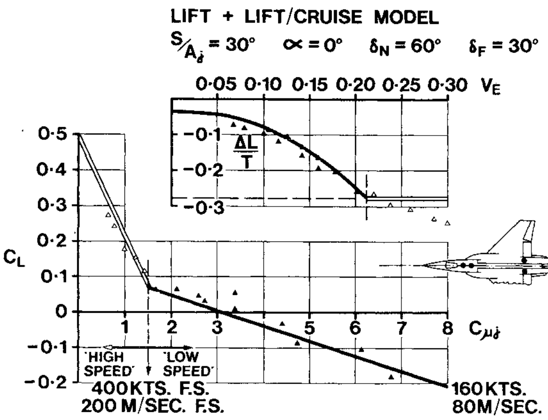


Fig. 14 Data presentation format.

where the fan thrust at forward speed has not been defined well enough to resolve the induced lift correctly.

In the following section, examples of the resolution and the aerodynamic characteristics that are revealed by these techniques are discussed.

Data Presentation and Aerodynamic Characteristics

Ground effect at forward speed on jet lift vehicles often produce some strange lift interference characteristics. Figure 12 shows some data taken shortly after the external wind screens were fitted. The test (same height, angle of attack, lift nozzle vector angles, etc.) was repeated later in the program when the external wind conditions were different. The characteristics of the lift and pitching moments are well-repeated.

Figure 13 shows some data taken from a single jet in wing research model where the effect of jet location chordwise on the planform was being investigated. The effect of rearward movement is clearly defined by the test data, permitting some empirical expression development to be undertaken. The characteristics of the rearward location are similar to some fan-in-wing results reported in the literature, i.e. a lift drop followed by some augmentation. It is worth noting that the presentation format used here exaggerates the lift differences at the high-speed end. In terms of the lift coefficient, the differences between the two jet positions are not nearly as significant as appears at first sight. This had led to the adoption of an additional format (Fig. 14). Some results of an investigation into the effect of lifting jets on a fighter planform are shown on Fig. 14. Here the data are presented in "lift as a proportion of thrust" and in "lift coefficient" form. The C_L vs C_μ plot includes the power-off lift at the origin and is a good method for inspecting the high-speed end of the transition maneuver.The other presentation highlights the hover and initial transition, where the direct jet thrust tends to dominate the scene and lift losses impact the takeoff weight and hence payload/range performance. As in the previous example, the data have been used to develop empirical methods for preliminary design application.

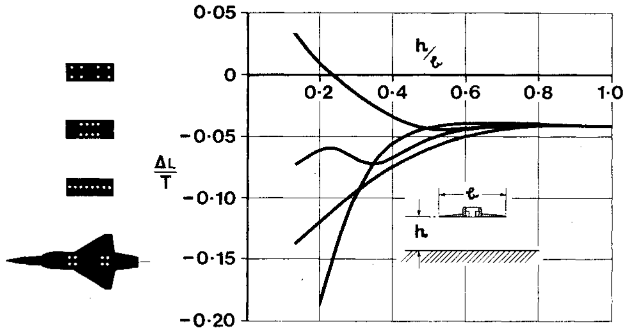


Fig. 15 Hover characteristics.

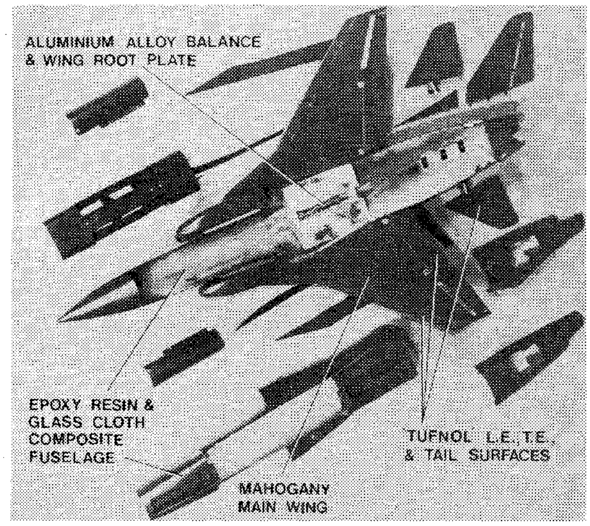


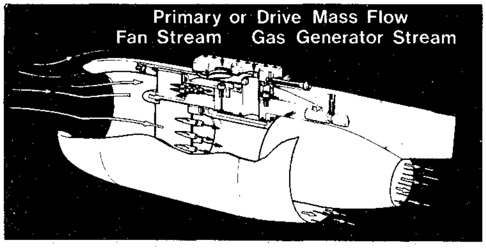
Fig. 16 Typical construction materials.

Hovering lift is depicted in Fig. 15. The interference lift is produced entirely by jet entrainment and, without a grounded thrust system, the subtle differences between the hovering losses in ground effect could be lost in thrust measurement accuracy. These differences are important as they impact directly on installed power required for a given vertical takeoff mission weight and, in this respect, only two of the four layouts tested here could be considered acceptable.

Model Fabrication Considerations

Some special considerations in model manufacture are of such vital importance to the technique as a whole that they merit some discussion.

First, because of the low freestream dynamic pressure and the exclusive use of internal strain-gage balances, it is very important to keep model weight down, otherwise the model weight dominates the balance load range with a consequent loss of resolution. The low weight demand does not, in



	Fan Stream	Gas Gen. Stream
A_R	24	29
Effective L/D	12 (62 Nozzles)	12 (7 Nozzles)

Overall Length 386 mm
Diameter 173 mm

Fig. 17 High by-pass-ratio engine simulator.

general, conflict with strength requirements but can cause stiffness problems. Second, powered-lift models tend to demand large internal volume for the propulsion simulator drive airfeed in addition to the simulators themselves, and an internal balance. Finally, powered-lift testing rquires the manufacture of engine simulators or blowing devices of one sort or another.

The first two special requirements have led to the extensive use of glass-fiber-reinforced plastic (GFRP) molding techniques. The internal volume and weight requirements are well met, together with good external profile control and finish. Selective metal reinforcing is, however, needed to give the desired stiffness and is also used for highly stressed items such as flap brackets and thin sections of flaps and slats. Figure 16 shows the airframe (metric) components of the V/STOL fighter model of Fig. 2. In addition to the materials mentioned, this particular model also uses hard wood (mahogany) and a compressed paper resin material known in the U.K. as 'Tufnol.' The model metric weight was 30 kg (66 lb) for a 1.2 m (4 ft) span one-eighth scale model.

With a good compressed air system, simulation of jet flow (without inlet flow) is relatively easy. The highest pressure available is used to reduce the size of ducts and plenum chambers and then the pressure is reduced across perforated plates into the final nozzle. The nozzle plates are designed to run choked over the test range and thus the mass flow is, for the most part, independent of the downstream pressure field. A static thrust calibration against supply pressure is thus considered valid in a forward speed test.

The airbreathing simulator requirement was first seriously studied at British Aircraft Corporation (BAC) in the late 1950's. Turbine-driven fans available at that time were mechanically and aerodynamically poor (mass flow ratio, secondary to primary, of less than 3). BAC decided, therefore, that the compressed air ejector would be a better option, i.e., cheaper, more reliable mechanically, and could be better tailored to each particular requirement. The design and build of our own ejector units was therefore initiated. With British Ministry of Defense support, a considerable amount of research into ejector design and performance has been undertaken.[5] Figure 17 shows one example, an RB 211 simulator, not built for V/STOL but built as a result of the technology initiated by V/STOL. This example has been chosen because it displays the versatility of the ejector. The engine and bypass streams are independently represented. Compared with modern turbofan units the inlet mass flow deficiency for high bypass ratio engines is greater at full-scale jet pressure ratio, but improves with reducing pressure (the way the V/STOL tests are carried out).

In manufacture, the ejector has presented us with some challenging problems, centered mainly around the primary nozzles. Most V/STOL engines are axially short, thereby demanding that the primary flow is subdivided in order to achieve optimum mixing. The manufacture of small multiple nozzle assemblies was initially solved by spark erosion techniques. Recently, more sophisticated nozzle shapes (condi supersonic nozzles) have been individually formed and accurately aligned in an assembly.

Concluding Remarks

None of the techniques discussed in this paper are unique, today. One or another of the techniques—strain-gage balances, metric and nonmetric components, reduced velocity scale, GFRP molding, or ejectors as engine simulators—are to be found in widespread use in the wind-tunnel practice. What is possibly unique is the way in which they have been blended together to form a cost-effective approach to powered-lift model testing. The key to this blend is, of course, the reduced velocity scale concept. This paper has, hopefully, brought out the benefits of this concept, but its validity in terms of Reynolds number and Mach number deficiency should continue to be questioned. Future research will help to define the technique's limitations in this respect.

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