Study of a Test Cell for Commercial Jet Engines

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Abstract

This study seeks to deepen the understanding of flow behaviour within the test cell of TAP. Common problems of test cells are described in the literature. The challenges that TAP has faced since 1969 and the ones TAP is facing currently were addressed. In order to validate the Test Cell Computational model - TCC - some pressure measurements were taken at the chamber’s entrance section and at the diffusor exit of the cell at TAP. Average values of the main properties were evaluated from the collected data, namely the mass flow rate. With the TCC model it is possible to foresee the test cell behavior operating with bigger engines and to quantify some alternative improvements for the facility.

1 Summary of the Work Done

A typical test cell structure is shown in Fig. 1. The intake section absorbs part of the noise generated and provides a uniform air flow to the chamber room, where the engine performance measurements are taken. The exhaust consists more often of an augmenter, a diffusor section followed by a boot section and an exhaust stack, which allows the diluted combustion streams to be expelled through the atmosphere.

Data from test runs was gathered starting from engines currently tested at TAP to accomplish a computational model basis, developed in MATLAB. Once validated through comparison between its estimates and the experimental data, the TCC model enabled flow behaviour prediction for an engine with higher dimensions. The effects of changing the diffusor and the Koppers harp configuration at the exhaust section were analyzed.

It was found that the actual layout of the exhaust section limits the capability of the test cell to test more powerful engines. To support this, the cell bypass ratio as well as the front cell approach velocity and the mixed gas temperature through the exhaust treatment were predicted for each of the engines selected for this study. All the simulations done refer to steady-state regimes at the takeoff rating. A set of recommendations for improvement of this test cell is presented, based on the extrapolation of the results achieved with the baseline model.

2 Typical Challenges of Test Cells

Test cells present some challenges that have to be addressed when aiming to test more powerful engines.

The components of the cell contribute to lower the cell pressure. To avoid significant flow distortion, this should be limited; Jacques [6] states that a difference between the ambient and the chamber static pressure up to 150 mm $H_2O$ (1470 Pa) is unlikely to be a problem for the engine work conditions and the correction factor of the measurements.
Resonance may also arise whenever the frequencies of the flow match any of the cell’s exhaust resonant frequency. The level of low frequency infrasound due to large scale turbulence is this way another challenge; those may be reduced by means of installing a ring diffusor, also known as a Koppers harp[8], as the one installed at TAP.

The peripheral flow \( \dot{m}_{\text{per}} \) is related to the the cell bypass ratio CBR and should be within certain limits.

Low CBRs may originate recirculation of the exhaust gases. Engine re-ingestion of the exhaust gases result from the pressure at the augmenter or at the exhaust stack being excessively high thus reducing the peripheral air flow. Vortex formation [2] is related to the deceleration of the peripheral air flow due to static pressure rise along the chamber. The peripheral-to-front cell velocity ratio \( v_{\text{per}}/v_2 \) should always be higher than 0.4 – 0.5 [2]. Low peripheral flows also mean a higher exhaust velocity and thus higher sound levels at the cell exhaust.

High CBRs means high flow velocities at the chamber’s periphery, increasing the noise at the chamber and also affecting the thrust correction factor too much, resulting on less reliable correlations.

Experience has shown that bypass ratios greater than 0.75 or 0.8 are acceptable i.e. to test an engine of a given thrust the air flow that must be handled by the test cell should at least be 1.8 times the engine air flow [3]. The CBR should be within the range of 0.8 and 2.0. The values obtained for the cfm and cf6 engines comprised in this study are presented at Table 1a.

3 The Test Cell of TAP

Fig. 1 schematizes and identifies the main sections of TAP’s test cell; all rakes of the test cell are signalized at the corresponding stations as well.

The Fig. 3a illustrates the engine hardware available in the test runs at TAP. The engine BPRs decrease from smaller to the larger engines considered herein, which are presented in the table below.

TAP’s test cell has been through structural reviews: at 1972, a perforated basket tube was installed to prevent flow separation at the diffusor which has lead to the P&W JT9D engines stall. At 1989, a wedge placed within the diffusor allowed to avoid flow separation. Moreover, the augmenter was not capable of uniformizing the flow sufficiently for the cf6-80C2 engines, leading to massive alternating flow separations at the diffusor. A Koppers harp and a set of grids were installed to assure a uniform velocity profile.

A rectangular diffusor fully equivalent to the diffusor at TAP was analyzed. The wedge at the diffusor reduces its area ratio the pressure-recover coefficient is smaller but flow separation is avoided. This amounts to reducing the divergence angle closer to the values at which best performances are reached. The diffusor with wedge operates at no stall conditions.

3.1 Head Loss Coefficients

Head losses due to the intake acoustic devices, the guide vanes that redirect the flow from the vertical intake stack and the grids placed upstream of the chamber section were estimated from correlations presented by Idelchik [5]. Similarly, head loss coefficients associated to the diffusor and the sudden expansion downstream, as well as to the exhaust grids and flow turning baffles were estimated from Idelchik. The methods used to estimate the coefficients that are presented at Table may be accessed through the source referenced therein. The majority of these coefficients are computed at the TCC code because depend (slightly) on the Reynolds number.

The experimental results have shown a mean value for \( k_{\infty-2} = 7.64 \) against the \( k_{\infty-2} = 7.56 \) predicted with the TCC model. The relative difference of 2.2% from the former is small and the model
was thus validated for the intake section of the test cell.

4 TCC Model

4.1 Model Approach

A computational model was developed for the computation of the cell air flow $\dot{m}_{\text{cell}}$ and the area occupied by the peripheral air flow at the augmenter. The head losses computed should match the ones obtained experimentally for model validation. Once known, extrapolation for larger engines may be attained.

The peripheral air flow is incompressible and part of it is entrained by the engine flow streams; the remaining flow is moved through the cell due to the augmenter pumping effect:

$$\dot{m}_{\text{per}} = \dot{m}_e + \dot{m}_m. \quad (1)$$

This air flow is defined by the augmenter inlet pressure; $p'_5$ is arbitrated to allow to discretize the velocity profile at $S_5$ for a fixed cell air flow $\dot{m}_{\text{cell}}$. From Bernoulli equation, the flow conditions at the intake are computed. The velocity $v_{m,5}$ is re-computed until the pressure mentioned converges. The flow properties at the diffusor inlet $S_6$ are computed from an energy balance. The static pressure at the exhaust room $p'_{7A}$ is then obtained from Bernoulli equation.

At the same time, the flow conditions downstream of the diffusor are dependent on the pressure loss at the exhaust. Both stations $S_1$ and $S_8$ are at the atmospheric hydrostatic pressure; this enables the to define $p'_6$ and $p'_{7A}$ easily again from Bernoulli equation.

The peripheral flow is incompressible but the flow entering the engine through the bellmouth duct suffers compressible effects. The aitken acceleration process enabled to accelerate the convergence of the thermodynamic flow properties at the engine intake.

4.2 Efficiency Parameters

The TCC model includes a factor $\xi$ that quantifies the velocity uniformization at the Koppers harp. The model also includes a factor $\zeta$ that quantifies the augmenter’s performance. $\zeta$ is the measure of the flow diffusion achieved at the end of the augmentor; its value was adjusted fit the available experimental data and then kept constant for all simulations. The quantities shown at Table 1b were directly compared with measurements and provided the basis for the model calibration: the pressure probes $p'_2$ and $P'_5$, the forward and rear static pressure $p'_{\text{forward}}$ and $p'_{\text{rear}}$, the static temperature and static pressure at the exhaust boot section $T_{7A}$ and $p'_{7A}$ and the exit cell temperature $T_8$. Some of the parameters of the TCC model were obtained from the engines manufacturers, e.g., the theoretical BPR (engine bypass ratio). Some correlations were found in the literature concerning the secondary core length $x_s/D_p$ and the head loss coefficients.

The test cell and the TCC model must verify the following relations and ranges: $R_a < R_{\text{aug}}, 0.75 < \text{CBR} < \text{CBR}_{\text{max}}, v_{3,\text{per}} > (v_{3,\text{per}})_{\text{min}}$ and $v_{m,5} < (v_{m,5})_{\text{max}}$, where $\text{CBR}_{\text{max}}$ is the maximum CBR recommended (around1.1 and 2.6 for large and small engines respectively [2]).
5 Entrainment Model

5.1 Potential Cores

Papamoschou [9] studied the interplay between the primary and the secondary engine streams; he concluded that the secondary core ends at an axial location normalized by the primary exit diameter of \( x_s/D_p \approx 4-7 \). With a higher momentum, the primary core may extend up to \((13-15)D_p\), as illustrated in Fig. 2a. From Fig 2a II it may be seen that the velocity profile does not change significantly from the fan’s discharge duct to the core nozzle exit. We can thus consider that the primary and the secondary flow begin at approximately the same vertical plane.

5.2 Flow Profiles

Any flow property is defined in polar coordinates at station \( S_{4C} \) i.e. \( x(S_{4C}) = 0 \) assuming axial symmetry. The ratio \( x/D_p \) up to which flow velocities are kept constant within the velocity profile along the \( x \) direction is be between 4.0 and 6.8 for the secondary flow and 13.3-15.0 and 16.0 for the primary one depending on the engine bypass ratio ([10], p. 4 and [9] p. 8).

The TCC model checks if the secondary potential core has finished before the augmenter’s entrance (case A, as Fig. 3b shows), or some of the flow is still potential (case B). The velocity profile at \( S_6 \) is sketched in the figure mentioned for the limiting case of a complete flow uniformization at the end of the augmenter. The pressure \( p_\alpha \) and the radii that characterize the evolution of the potential cores at augmenter’s inlet, i.e. \( R_{p,1}, R_{s,2}, R_{s,1} \) and \( R_a \), are obtained iteratively in the TCC model as a function of the engine air flow \( \dot{m}_{eng} \) and the engine BPR.

5.3 Thermodynamic Flow Properties

For each test run data collected at TAP, MATLAB computes the actual

\[
A F R = \left( \frac{m_a}{m_f} \right),
\]

the air to fuel ratio of the primary flow. The dilution is obtained as \( \frac{A F R}{(A F R)_{s}} \) (\%) and is used to compute the actual specific heat and the ideal gas constant of the exhaust gas, \( cp_{exh} \) and \( R_{exh} \). It was verified that the temperature rise due to combustion changes very little the ideal gas constant of the combustion products, but changes significantly the specific heats \( cp \) and \( cv \).

The flow properties at the diffusor, at \( S_6 \), are evaluated through an energy balance. After station \( S_6 \), the fluid properties \( cp(r) \) and \( \rho(r) \) are assumed uniform over the cross section. The mean flow properties at \( S_5 \) are defined as follows:

\[
\begin{align*}
\bar{v}_5 &= 2\pi \left[ v_p R_{p,1}^2/2 + \int_{R_{p,1}}^{R_{s,2}} v(p) dp + v_s (R_{s,1}^2/2 - R_{s,2}^2/2) + \int_{R_{s,1}}^{R_a} v(r) dr \right]/A_{aug} \\
\bar{\rho}_5 &= 2\pi \left[ \rho_p R_{p,1}^2/2 + \int_{R_{p,1}}^{R_{s,2}} \rho(p) dp + \rho_s (R_{s,1}^2/2 - R_{s,2}^2/2) + \rho_m (R_{aug}^2/2 - R_a^2/2) \right]/A_{aug} \\
\bar{T}_5 &= 2\pi \left[ T_p R_{p,1}^2/2 + \int_{R_{p,1}}^{R_{s,2}} T(p) dp + T_s (R_{s,1}^2/2 - R_{s,2}^2/2) + T_m (R_{aug}^2/2 - R_a^2/2) \right]/A_{aug} \\
\bar{cp}_5 &= 2\pi \left[ cp_p R_{p,1}^2/2 + \int_{R_{p,1}}^{R_{s,2}} cp(p) dp + cp_s (R_{s,1}^2/2 - R_{s,2}^2/2) + cp_m (R_{aug}^2/2 - R_a^2/2) \right]/A_{aug}.
\end{align*}
\]

(3)
5.4 Ejector Pump Effect

The volume flow rate per unit area entrained in the augmenter is

\[
\left\{\begin{array}{l}
\frac{Q_{\text{per}}}{A_{\text{tot}}} = \frac{\nu_{\text{per}} \cdot AR \cdot A_{\text{eng}}}{A_{\text{tot}}} = \nu_{\text{per}} \cdot AR (AR + 1)^{-1} , \\
AR = \frac{A_{\text{per}}}{A_{\text{eng}}}
\end{array}\right.
\]

(4)

where AR is the peripheral flow area to engine flow area ratio. Turbofans have two distinct flow streams instead of a single primary flow. This favours the momentum diffusion. The turbofan engines considered in our study are expected to behave approximately as in Fig. 2b. This figure is in agreement with [2], who presented empirical relations for the entrainment ratio1:

\[
\left\{\begin{array}{l}
ER = 0.22 \frac{D_{\text{aug}}}{D_n} + 1.07 \\
ER = 0.9304 \frac{D_{\text{aug}}}{D_n} + 1.1892
\end{array}\right.
\]

(5)

From Fig. 2a, if \((L/D)_{\text{aug}} = 8\) corresponds to 100%, a value of \((L/D)_{\text{aug,TAP}} \approx 4\) leads to a performance factor of \(\psi = 3.2/4.4 = 73\%\). The performance factor is function of the mass flows \(\psi = \psi(Q_{\text{per}}/Q_{\text{eng}})\), as defined by Quinn [12]. The geometry of the augmenter determines the amount of peripheral cool air flow being entrained: the depression it allows is key factor controlling that air flow.

6 Results and Discussion

Historical information about TAP’s test cell since 1972 until its current configuration has been collected. This survey was useful to understand the thresholds of the test cell capacity. This background information was important to develop the numerical model TCC of the whole plant, with which it was possible to identify a set of practical provisions that would make its upgrade achievable. The results obtained with the TCC model are presented in Table 1.

The present research verified that, as engine size increases, the amount of peripheral air flow becomes critical for the same size and geometry of the test cell. With the current test cell configuration, a test run of the cf6-80E1 would yield a very small CBR value, as can be deduced from Fig. 2b. Less peripheral air flow being pumped may result in vortices formed upstream of the engine intake, that may lead to engine surge. Therefore, increasing the CBR seems a first solution to the present test cell limitations. Nevertheless, other solutions were found, such as placing ramps around the walls of the chamber’s station \(S_3\) see Fig. 4). According to this approach, a smaller CBR would be acceptable, with the advantage that the pressure downstream of the engine and at the augmenter’s inlet would be closer to atmospheric conditions. This change would reduce the amount of correction needed when measuring the engine thrust.

A suggestion is to eliminate the wall in the plane of the current inlet section of the augmenter to increase the distance between the engine and the augmenter. This change would improve the thrust measured (less correction needed) and increase the CBR without lowering the inlet pressure of the augmenter. Furthermore, separating the engine from the augmenter’s inlet would compensate an hypothetical need of a greater CBR.

This study shows that the augmenter does not fully uniformize the velocity and temperature profiles along its length, not even with a Koppers harp installed. With an uniformization rate of \(\zeta = 0.7\), the TCC model matches the measured \(\dot{m}_{\text{cell}}\) and \(p'_{\text{A}}\). Fig. 4 shows the mass flow rate \(\dot{m}_{\text{cell}}\), the augmenter inlet static pressure \(p'_0\), the augmenter exit static pressure \(p'_0\) and the exhaust static pressure \(p'_{\text{A}}\) as function of the augmenter uniformization rate \(\zeta\). From Fig. 4, for \(\zeta = 1\) (full velocity uniformization),

\footnote{The constant 1.07 in the first correlation appears in the original document as 10.7, but we believe this is a typo, according to all the other information provided by the authors.}
the static pressure $p'_{7A}$ and the mass flow rate estimated are about 5.4% and 1.6% higher than for $\zeta = 0.7$. The length of the present augmenter is too short to obtain a fully uniformized flow velocity; a ratio $(L/D)_{aug} = 8.0$ would be recommended. Increasing its length would be a benefit for testing larger engines as the -E1. The computational model shows that both the augmenter and the diffusor could have a larger cross sectional area. In this case, the augmenter should be even longer to achieve the optimal ratio $(L/D)_{aug} = 8.0$.

From the CBR viewpoint, there is no great advantage on using a Koppers harp, because it negatively affects the pumping performance of the augmenter. We may conclude from Fig. 3 that the five outer rings of the Koppers harp are ineffective. The removal of these rings would allow the augmenter to pump a higher peripheral flow, as needed by the cf6-80E1.

The flow in the diffusor would improve replacing the wedge by a set of metallic guide vanes. The present wedge avoids a bi-stable flow separation (that would occur with the wide-open geometry prior to the wedge) and reduces an otherwise excessive pressure recovery, however this is an inefficient way of stabilizing the flow and does not provide an adjustable control of the peripheral flow rate.

The head loss at the exhaust room imposed by the screen structures at $S_{7B}$ counteracts the augmenter’s pumping effect because of the head loss.

Another set of guide vanes installed at each turning of the exhaust stack would provide a better flow stabilization and avoid resonance, which is still an issue, despite of the last improvements of the test cell.

7 Future Work

A computational model for this test cell was developed for its current configuration and was compared against experimental data. It provides a useful tool to analyze the impact of parametric changes of the test cell. Unfortunately, the flow conditions during a -C2 engine testing could not yet be measured. It would be of great value to obtain that data, to further substantiate the accuracy of the TCC model. The engine test runs sampling is quite short. Obtaining a more representative number of test runs (for the -3C and -C2 engines mainly) could also improve the accuracy of the TCC model.

An acoustic study could evaluate the near field and far field noise levels associated with cf6-80E1 engine operation. Acoustic measurements could provide further information about the flow stability (the main resonance aerodynamic frequencies). This way, we could foresee whether the test cell would operate properly for the cf6-80E1 engine.

A small-scale study of the test cell could quantify its aerodynamic behavior when operating with -E1 engines, or even with P8/W4168 and the Trent-1000 engines, looking forward to the near future of TAP’s maintenance services fostering, even strongly, its competitiveness.

References


Figure 1: Installed rakes at the test cell. Upper image: figure taken and adapted from [7].
(a) Engines analyzed in the study (images from [1] and [11]).

(b) Geometric input data for the TCCM model.

(a) I) Potential core lengths; II) Velocities distribution (BPR = 5.0).
(b) Nozzle and collector size effect on the CBR.[4].

(a) CBR and cell depression of the engines comprised in the study.

(b) The different types of parameters defining the computational model TCCM.
(a) Augmentation performance on mixing augmenter length. Here, $\pi$ is the primary-to-ambient pressure ratio. (source: Quinn, [12]).

(b) Trendline for the entrainment flow ratio as function of the engine size for the test cell at TAP.

Figure 2: CBR trendline comparison between different author studies for turbojet engines.

(a) Turbofan dress kit used in test cells (SAFRAN - Cencio InternationalTM)

(b) Primary, secondary and entrained flow streams.

(a) Harp structure: rings effectively installed and rings already removed at 1989 (adapted from [7]).

(b) Harp rings removal with -E1 engines.

Figure 3: Effect of harp rings removal on the cell mass flow rate and CBR.
Figure 4: Left: Inlet ramp structures. Right: Pressure and mass flow rate as a function of the velocity uniformization $\zeta$ along the augmenter (results obtained with the TCC model for the -3C engine, based on the test run data of 03-Oct-2014, with the Koppers harp installed).

Table 1: Engine test runs selected from the ones obtained between 20/Aug/14 to 14/Apr/15, at TAP’s test cell facility.