Optimization of Intercooled Turbofan Jet Thermodynamic Cycle Considering Weight Penalty and Pressure Loss of Heat Exchanger

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Thermal cycles of a turbofan jet engine equipped with an intercooler are optimized for two different engine sizes to understand the engine characteristics. The optimization tool consists of a thermodynamic cycle analysis module, a weight evaluation module, a heat exchanger model and an optimization routine. It is confirmed that the tool searches for a reasonable design point within a ten-dimensional design space. The pressure ratio of a conventional turbofan is restricted by the compressor exit temperature. In contrast, intercooling enables higher pressure ratios and hence higher core thermal efficiency. The net decrease in fuel consumption is small however, because the thermal efficiency improvement, weight penalty and pressure loss are all at the same order of several percent. The minimum blade height at the compressor exit imposes another restriction on the pressure ratio increase for small intercooled engines, and net fuel consumption in small engines may increase by intercooling. The performance of an intercooled turbofan is determined by a balance between thermal efficiency improvement through the increase in pressure ratio and disadvantages resulting from additional weight and pressure losses. The development of a light, low-pressure-loss heat exchanger and optimization of the thermodynamic cycle are important.

Key Words: Jet Engine, Thermodynamic Cycle, Intercooling, Optimization

1. Introduction

Almost all commercial airplanes are equipped with middle- to high-bypass-ratio turbofan jet engines. The performance of turbofan jet engines has been continuously improved through (1) improvement in component efficiencies, (2) improvement in propulsion efficiency resulting from an increase in bypass ratio and (3) improvement in core cycle efficiency resulting from an increase in the overall pressure ratio. Meanwhile, its thermodynamic cycle has remained the same since the very first model, in which the core engine relies on a simple Brayton cycle and a low-pressure turbine directly drives the fan. Today, efficiencies of major engine components reach approximately 90% and the scope for further improvement is limited. Increasing the bypass ratio is restricted by the clearance between the wing pylon and the ground. The weight penalty accompanied by any increase in low-pressure turbine stages is another concern in increasing the bypass ratio. An increase in the overall pressure ratio inherently results in an increase in the combustion temperature; therefore, it is limited by material and NOx emissions. These circumstances provide the motivation for research efforts, attempting to realize further improvement by modifying the thermodynamic cycle itself using a heat exchanger.

To improve the jet engine thermodynamic cycle using a heat exchanger, two strategies have been pursued, one is an intercooled (IC) engine and the other is an intercooled and recuperated (ICR) engine. In an intercooled engine, airflow from an intermediate-pressure compressor is introduced into an intercooler where it is cooled by cold bypass airflow before flowing back into the high-pressure compressor. In addition to the intercooler, an intercooled and recuperated engine has another heat exchanger in which thermal energy is exchanged between the low-pressure turbine exhaust gas and the combustor inlet airflow. The concepts of IC and ICR engines are well documented in textbooks, but they have never been introduced in product engine design owing to the complexity of the flow path and the weight penalty of the heat exchanger.

Studies in the late 2000s relating to the NEWAC project in Europe showed that recent advances in heat exchangers make an IC engine more feasible than ever because the flow paths of IC engines are much simpler than those of ICR engines and the reduction in the core engine weight owing to improvement in specific thrust may offset part of the intercooler weight penalty. From this point of view, there has been active research in this field. Kyprianidis studied IC and ICR engines for commercial airplanes and showed that intercooling enables higher design pressure ratios and enhances the core specific power, resulting in better core thermal efficiency and lighter core weight. They also showed that the recuperation greatly reduces fuel consump-
tion but at the cost of total engine weight. Xu et al.\textsuperscript{3)} optimized both the thermodynamic cycle and the intercooler of a small (120 kN, 27,300 lb thrust) turbofan engine and showed that the design pressure ratio of the optimized IC engine increases to 61, compared to 41 for the optimized conventional turbofan engine. Fuel consumption of the optimized IC engine was 4.1% lower than that of the optimized conventional engine. The heat exchanger used in their study was a circular tube heat exchanger, whose temperature effectiveness was 29% and whose hot side pressure loss was 10%. Rolls-Royce\textsuperscript{4)} analyzed the performance and pressure loss of heat exchangers and ducting for 140- and 320-kN class IC engines. Based on its study, the company proposed arranging 20–24 modules of cross-corrugated primary surface heat exchangers along the outer annulus of the high-pressure compressor and the combustor. The temperature effectiveness of such heat exchangers was predicted to be 60–70%. Papadopoulos and Pilidis\textsuperscript{5)} discussed the feasibility of a 400-kN-class IC engine using heat pipes. Their studies showed that some improvement in specific fuel consumption (SFC) can be expected by intercooling, but the weight penalty of the heat pipe offsets the SFC advantage and overall fuel consumption increases.

There are also active research studies on the intercooler,\textsuperscript{6–9)} and performance assessment.\textsuperscript{9,10)} Although each author discussed the intercooling characteristics for a chosen set of engine specifications, the conclusions were not the same. For example, fuel consumption improvement ratios, weights and temperature effectiveness of the intercooler differed among reports. These studies showed that various design parameters affect engine performance in various ways and also that the effect of intercooling may depend on the target engine specifications such as thrust or specific thrust.

In this study, the characteristics of intercooling, especially its effect upon fuel consumption and component weights, are discussed through a comparison between an optimized conventional engine cycle and an optimized intercooled engine cycle. The difference in optimized thermodynamic cycles between different engine sizes is also discussed.

2. Numerical Modeling

2.1. Target engines

An intercooled three-shaft turbofan jet engine (Fig. 1) that has an intercooler between its intermediate-pressure compressor (IPC) and its high-pressure compressor (HPC) will be studied.

The performance of the intercooled engine strongly depends on intercooler performance. Here, a cross-corrugated primary surface heat exchanger studied by Rolls-Royce\textsuperscript{4)} (Fig. 2(a)) is used instead of the tube-type heat exchanger used by Xu et al.\textsuperscript{3)} Because the primary surface heat exchanger has a higher surface-to-volume ratio, it generally has better temperature effectiveness compared with a tube-type heat exchanger. The heat exchanger is packed into modules, as shown in Fig. 2(b), and arranged along the outer annulus of the high-pressure compressor, as shown in Fig. 1.

Two engine classes—the 26,000 lb class and the 95,000 lb class—are studied in this report and their specifications are listed in Table 1.

2.2. Design parameters, constraints and objective functions

The design parameters—five parameters for the conventional turbofan and ten parameters for the intercooled turbofan—used to evaluate engine performance are listed in Table 2.

The fuel consumption rate at cruise conditions, which is
the objective function in the optimization, is evaluated in the following manner.

1. Evaluate design point performance at the sea-level static condition.
2. Scale the engine to fit the target thrust shown in Table 1. Mach 0.85 cruise condition of the scaled engine.

The cruise thrust $F_{\text{cruise}}$ is evaluated from the maximum takeoff weight of the aircraft, $W_{MTO}$, the number of engines, $N_E$, the cruise lift-to-drag ratio $L/D$, and baseline engine weight $W_E$ using the following equation.

$$F_{\text{cruise}} = \frac{W_{MTO}}{N_E \cdot L/D} + N_E \left( \frac{W_E - W_{E\text{base}}}{W_{MTO}} \right).$$

$F_{\text{cruise}}$ reflects the weight penalty of the engine; therefore, the cruise fuel consumption rate evaluated in step 3 also reflects the weight penalty. In real applications, block fuel is more suitable for the objective function of small engines; however, cruise fuel consumption is used for both classes for simplicity.

Constraints on the cycle analysis (three for the conventional turbofan and six for the intercooled turbofan) are listed in Table 3. Here, it must be noted that the upper bound of the fan diameter decides the lower bound of the specific thrust because the total thrust is fixed to that of the base engine. Therefore, the fan pressure ratio tends to be higher in the optimization.

### 2.3. Analysis models

The analysis model consists of a thermodynamic cycle analysis model that can evaluate design and off-design point performance, a heat exchanger model that can evaluate intercooler performance and a weight model that can evaluate the weights of engine components.

| Table 1. Engine class. | Class | Thrust [kN] | Dia. [m] | Model |
|------------------------|-------|-------------|---------|-------|
| Baseline engine weight [kg] | | |
| Thrust [kN] | Dia. [m] | Model |
| 26,000 lb | 115.7 | 1.549 | CFM56 |
| 95,000 lb | 424.8 | 2.794 | Trent 895 |
| Takeoff weight [kg] | Baseline engine weight [kg] | No. of engines | Model |
| 77,600 | 2,386 | 2 | B737-700ER |
| 297,800 | 5,986 | 2 | B777-200ER |

| Table 2. Design parameters. | Parameter | Min. | Max. |
|-----------------------------|-----------|------|------|
| Fan pressure ratio | 1.4 | 1.8 |
| IPC pressure ratio | 3.0 | 12.0 |
| HPC pressure ratio | 3.0 | 25.0 |
| Bypass ratio | 3.0 | 10.0 |
| TIT [K] | 1.500 | 1.900 |
| IPC/iPC pressure ratio | 0.05 | 0.7 |
| IPC width ($b$, Fig. 2(b)) | 0.01 | 1.2 |
| IPC height ($h$, Fig. 2(b)) | 0.01 | 1.2 |
| IPC length ($d$, Fig. 2(b)) | 0.1 | 2.5 |
| Number of IC modules | 1 | 50 |

| Table 3. Constraints. | Parameter | Limit |
|-----------------------|-----------|-------|
| Min. blade height at stn. 3 | 15 mm |
| Max. temp. at stn. 3 | 950 K |
| Max. fan diameter | Table 1 |
| Max. IC module height | $1/(\text{fan tip dia.} - \text{IPC tip dia.})$ |
| Max. IC module axial length | $\text{HPC length} + \text{combustor length}$ |
| IC module circum. width | $\text{HPC circum. length}$ |
| Number of modules | |

| Table 4. Component efficiencies. | Parameter | Value |
|----------------------------------|-----------|-------|
| Intake pressure ratio | 0.99 |
| Fan polytropic efficiency | 0.89 |
| Compressor polytropic efficiency | 0.89 |
| Turbine polytropic efficiency | 0.90 |
| Combustion efficiency | 0.98 |
| Combustor pressure ratio | 0.95 |
| Lower heating value [MJ/kg] | 43.323 |
| Nozzle efficiency | 0.985 |
| Spool mechanical efficiency | 0.99 |
| Nozzle thrust coefficient | 0.99 |
| Cooling bleed [%] | $(0.03 \times \text{TIT}(K) - 35)$ |

In this study, all the models are constructed based on publicly available literature.

### 2.3.1. Thermodynamic cycle analysis

The engine flow path is divided into stations, as shown in Fig. 3, and the engine performance is evaluated from the conservation of mass and energy at each station. The component efficiencies are fixed to the values listed in Table 4. The secondary air bleed ratio at the HPC exit is given as a function of the turbine inlet temperature. The isobaric specific heat of the gas in each component is given as a fourth-degree polynomial function of the mean temperature between the inlet and outlet.
All the cooling air is extracted from the HPC exit, and 35% of the bleed air is used to cool the high-pressure turbine (HPT) nozzle, with 40%, 15%, 2% and 8% being used to cool the HPT blade, the intermediate-pressure turbine (IPT) nozzle, the IPT blade and the low-pressure turbine (LPT), respectively.

The enthalpy drop at the turbine is given as

$$H_{\text{HPT}} = \frac{H_{\text{HPC}} \cdot (W_3 + W_{25})/2}{n_{\text{HPT}} (W_{41} + W_{43})/2},$$

where $H$ is the enthalpy, $W_3$ and $W_{25}$ are the mass flow rates at the HPC inlet and outlet, and $W_{41}$ and $W_{43}$ are the mass flow rates at the HPT inlet and outlet, respectively. $n$ is the adiabatic efficiency. The denominator of the equation means that half of the cooling air does not contribute to the turbine work output.

The mechanical shaft speeds and operating points for the off-design condition are evaluated using an error-matrix method and assuming corrected mass flow–pressure ratio maps and corrected mass flow–efficiency maps for the fan, the IPC, the HPC, the HPT, the IPT and the LPT.\(^{11-16}\)

These design and off-design thermodynamic cycle analysis routines are validated through comparison with the commercial tool GasTurb.\(^{17}\)

2.3.2. Heat exchanger model

The primary surface heat exchanger consists of sinusoidal metal plates (corrugated fins), as shown in Fig. 2(a). The corrugated fins are piled up alternately at two different angles. High- and low-temperature fluids flow between the gaps in the corrugated fins alternatively and exchange heat.

Figure 2(b) shows the heat exchanger module. Values of $D_i$/fin height for corrugated fins of length $L$ and width $B$ are stored in one module. Hot core fluid exits the IPC and flows into the module from its top and is exhausted from the bottom. The cold bypass fluid flows into the module from the front and is exhausted backward.

In the present study, the fin shape is determined following Utriainen and Sunden\(^{18}\) and the fin height is set to 4 mm based on a design study by Sumitomo Precision Products.\(^{19}\)

The heat transfer coefficient $h$ and friction factor $f$ of the fin are estimated from experiment of Stasiek et al.\(^{20}\) as follows.

$$h = f(\mu) \cdot C_p \cdot Pr^{-2/3},$$

$$f = 0.031441 (Re/2028.48)^{-1/3},$$

$$j = 0.0717855 \sqrt{2028.955/Re}.\quad (2)$$

The fin surface area is determined from the module dimension and fin shape, and then the temperature coefficient of the intercooler is evaluated using the E-NTU method.\(^{21}\)

The pressure loss is given as

$$\Delta P = \frac{1}{2} \mu W \left( \frac{A_W}{D_e} + 0.25 \right) + 0.05 P_{in},\quad (3)$$

where $W$ is the channel width, $D_e$ is the equivalent diameter, 0.25 in the second term of the right-hand side corresponds to the pressure loss owing to passage contraction and expansion, and 0.05 in the last term represents the pressure loss of the connection ducts.

2.3.3. Weight and dimension model

The inner and outer diameters of compressors and turbines are evaluated from the mass flow rate by assuming a prescribed inner–outer diameter ratio and a specific mass flow. The number of stages is determined from the temperature difference per stage, $\Delta T$, mean radius peripheral speed $U_{\text{mid}}$ and aerodynamic loading factor $\psi$ as

$$\psi = \frac{C_p \Delta T}{U_{\text{mid}}^2}.\quad (4)$$

Component weights are evaluated from NASA-TM-X-2406\(^{22}\) using component dimensions. The weight of the intercooler is evaluated as

$$W_{\text{intercooler}} = 2.0 \times S \times \rho \times t,$$

where $S$, $\rho$ and $t$ are the surface area, density and fin thickness of the intercooler, respectively. The weight of the connecting duct is assumed to be the same as the weight of the intercooler itself, so a multiplication factor of 2.0 is used to take this into account. Here, $\rho \times t$ is assumed to be 0.4 kg/m\(^2\), which is considerably lower than that of current intercoolers made of stainless steel.

2.4. Optimization

An evolutionary algorithm\(^{23,24}\) is used to search for the optimum design point within a ten-dimensional parameter space. The CHC\(^{25}\) algorithm is used for the selection, the Unimodal Normal Distribution Crossover\(^{26}\) is used for the crossover, and random mutation is accounted for using Wright’s model.\(^{27}\)

The initial population is 200 for optimization of the conventional turbofan, and 2,000 individual points are used for optimization of the intercooled turbofan. Figure 4 compares the convergence history for different initial populations.

Optimization using many initial population points shows faster initial convergence, and 2,000 initial population points reached within +1% of the final solution after 8,000 generations. Random mutation becomes dominant and the convergence rate slows down after 10,000 generations, but all the cases converged within +0.1% of the final solution by 150,000 generations.

![Convergence history](image-url)
### 3. Results

#### 3.1. Optimization of the conventional turbofan cycle

The optimized design parameters of conventional turbofans are listed in the left column of Table 5. Contours of specific fuel consumption (Fig. 5(a)), engine weight (Fig. 5(b)) and the objective function (cruise fuel consumption, Fig. 5(c)) are plotted around the optimum point of the 26,000 lb class for varying turbine inlet temperatures and high-pressure compressor pressure ratios.

The fundamental characteristics of the Brayton cycle are observed in Fig. 5(a). For example, specific fuel consumption reaches a local minima at a certain pressure ratio, a higher turbine inlet temperature gives a higher optimum pressure ratio, and a higher pressure ratio or lower turbine inlet temperature results in lower specific thrust (and hence lower specific fuel consumption).
larger fan diameter). The engine weight contour in Fig. 5(b) is slightly complicated. The two discontinuities running vertically in the upper left from (overall pressure ratio (OPR) = 48 and turbine inlet temperature (TIT) = 1,800 K) correspond to stepwise changes in the number of HPC and HPT stages, respectively. Because Fig. 5(b) is plotted for varying HPC pressure ratio, higher OPR values mean higher core loading, and hence, a higher OPR naturally requires more HPC and HPT stages. Other features observed in Fig. 5(b) are the gradual weight increase from upper left to lower right, the three discontinuities running to the upper right from OPR = 44 and TIT = 1,800; 800 K, and from the optimum point indicated by a white circle. These features are correlated with the LPT expansion ratio and LPT mean diameter. Because LPT loading is kept constant in Fig. 5, a lower LPT inlet temperature results in a larger LPT expansion ratio, which leads to a larger LPT mean diameter and a higher mean peripheral speed. The turbine weight is proportional to its mean diameter to the power of 2.5. Therefore, the LPT expansion ratio is correlated with the LPT weight. The expansion ratio also has a minor effect on the adiabatic efficiency, decreasing the component temperature decrease. Given that the number of stages is determined from the component temperature decrease and the mean peripheral speed (Eq. (4)), variation of the LPT expansion ratio causes a step change in the number of LPT stages.

Because cruise fuel consumption is a combination of specific fuel consumption (Fig. 5(a)) and weight penalty (Fig. 5(b)), Fig. 5(c) shows a complicated variation. Nevertheless, the present tool successfully searches for the correct optimum point. Note that the optimum point is not at the intersection of two constraints (T_{31} < 950 K and fan dia. < 1.549 m).

In contrast, the cruise fuel consumption of the 95,000 lb class conventional turbofan shown in Fig. 6 varies without discontinuity, and the optimum point corresponds to the intersection of two constraints (T_{31} < 950 K and fan dia. < 2.794 m).

### 3.2 Optimization of intercooled turbofan cycle

The optimized design parameters of intercooled turbofans are listed in the right column of Table 5. The fan pressure ratio and T_{31} reach upper limits for both 26,000 lb and 95,000 lb, and the turbine inlet temperature and the fan diameter of 95,000 lb are also at their upper limits. These are the same results as found for the optimization of the conventional turbofan. However, the optimized parameter for the 26,000 lb class intercooled turbofan is limited by the HPC exit minimum blade height, which did not reach its limit for the conventional turbofan.

Figures 7 and 8 show contours of cruise fuel consumption plotted against the HPC pressure ratio and intercooler bypass ratio. Here, the intercooler bypass ratio is the ratio between the mass flow of the bypass air and that of the intercooler cooling air. The optimum point reaches the border of the HPC exit temperature upper limit (T_{31} = 950 K) for both cases. Contour lines for T_{31} = 950 K suggest that a higher intercooler bypass ratio is preferable because it allows a higher cycle pressure ratio and hence higher thermal
efficiency while keeping $T_{31}$ below the limit. However, the cruise fuel consumption along the $T_{31} = 950$ K line reaches its minimum at a relatively low intercooler bypass ratio.

The bypass nozzle pressure $P_{16}$ that produces most of the thrust is a mass-weighted average pressure of the fan exit pressure $P_{13}$ and intercooler exit pressure $P_{14}$. Because $P_{14}$ is lower than $P_{13}$, increasing the intercooler bypass ratio directly increases the bypass pressure loss and reduces the thrust. Contour lines of the fan diameter in Figs. 7 and 8 show that the specific thrust decreases owing to intercooler pressure loss in the higher intercooler bypass ratio region.

The discontinuity in the fuel consumption caused by the change in number of turbine stages discussed in Fig. 5(b) is also observed in Fig. 7.

The performance of intercooled turbosfans is influenced by many factors, and it is more complicated than that of conventional turbosfans. Nonetheless, correct optimum design points are successfully found with the present tool.

3.2.1. Effect of intercooling

Optimized intercooled turbosfan cycles are compared with optimized conventional turbosfan cycles in this section.

The overall pressure ratio of the optimized 95,000 lb class intercooled turbosfan is increased up to 93.5 from 48.2 for the optimized conventional turbosfan. The bypass ratio is also increased from 5.65 to 5.83, resulting in a 2.6% improvement in specific fuel consumption. As discussed in the previous sections, the upper limit of the HPC exit temperature restricts the overall pressure ratio. Intercooling at the HPC inlet enables a higher overall pressure ratio without increasing the HPC exit temperature, and thus it contributes to the improvement in specific fuel consumption. However, the weight penalty offsets approximately half of the specific fuel consumption improvement, and the resulting net fuel consumption improvement is only 1%.

In contrast, the overall pressure ratio of the smaller 26,000 lb class intercooled turbosfan is restricted not only by the HPC exit temperature but also by the HPC exit minimum blade height, and the overall pressure ratio only increases up to 67.6. The pressure losses at the bypass duct and the intercooler surpass the thermal efficiency improvement owing to such a small pressure ratio increase, and the specific fuel consumption increases by 0.2%. Net fuel consumption with the weight penalty taken into account increases by 0.65%.

Intercooling reduces fuel consumption through thermal efficiency improvement owing to the pressure ratio increase. Therefore, larger engines in which the HPC exit blade height does not restrict the pressure ratio increase are advantageous for intercooling.

3.2.2. Impact on weight

Figure 9 compares the weight of each component between conventional and intercooled turbosfans.

The weight of the core compressor (IPC + HPC) of intercooled turbosfans is slightly less than that of conventional turbosfans, although the core pressure ratios of intercooled turbosfans are considerably higher than those of conventional turbosfans.

However, the weight of the turbine, which accounts for a 40% share of the total engine weight, is greater in intercooled turbosfans, and the total engine weight excluding the intercooler is almost the same for conventional and intercooled turbosfans. The total engine weight of an intercooled turbosfan including the intercooler is naturally greater than that of a conventional turbosfan.

The weights of the intercooler are 25% of the total engine weight for the 95,000 lb class and 10% of the total engine weight for the 26,000 lb class. The weight penalty on the net fuel consumption is evaluated to be 1.1% and 0.5%, respectively. The surface density of the intercooler material is assumed to be 0.4 kg/m², which is considerably lower than that of current standard heat exchangers. But the weight penalty of the intercooler is still the same order as the SFC improvement from intercooling. The weight of the intercooler is a major concern in the design of intercooled turbosfans.

4. Concluding Remarks

A thermodynamic engine cycle analysis tool, a weight prediction model, a heat exchanger model, and an optimization routine were integrated into an engine cycle design tool. It was applied to an optimization of conventional and intercooled turbosfan engine cycles and successfully demonstrated that it can search for a reasonable optimum design point.

Examination of the optimized results revealed that the pressure ratio of a conventional turbosfan engine is restricted by the compressor exit temperature. Intercooling enables a higher pressure ratio for the same temperature limit, and the higher pressure ratio results in improvement in core thermal efficiency. Comparison between optimized results for two different engine sizes showed that the compressor exit blade height imposes another restriction on the overall pressure ratio of a small engine; therefore, a larger engine is advantageous for intercooling.

Two concerns in the design of intercooled turbosfans were
the weight penalty and the bypass pressure loss. The total engine weight of the intercooler turbofan was estimated to be 10% to 25% greater than that of the conventional turbofan. The weight penalty and the pressure loss penalty evaluated from the model were both of the same order as the specific fuel consumption improvement.

The performance of an intercooled turbofan was determined by a balance between thermal efficiency improvement through the increase of pressure ratio and disadvantages resulting from additional weight and pressure losses. The development of a light, low-pressure-loss heat exchanger and optimization of the engine cycle are important.

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