High-Bandwidth, High-Efficiency Envelope Tracking Power Supply for 40W RF Power Amplifier Using Paralleled Bandpass Current Sources

Høyerby, Mikkel Christian Wendelboe; Andersen, Michael Andreas E.

Published in:
IEEE 36th Conference on Power Electronics Specialists, 2005

Link to article, DOI:
10.1109/PESC.2005.1582030

Publication date:
2005

Document Version
Publisher's PDF, also known as Version of record

Link back to DTU Orbit

Citation (APA):
High-Bandwidth, High-Efficiency Envelope Tracking Power Supply for 40W RF Power Amplifier Using Paralleled Bandpass Current Sources

Mikkel C. W. Høyerby
Technical University of Denmark
Building 325
Dk-2800 Lyngby
Denmark
mwh@oersted.dtu.dk

Michael A. E. Andersen
Technical University of Denmark
Building 325
Dk-2800 Lyngby
Denmark
ma@oersted.dtu.dk

Abstract - This paper presents a high-performance power conversion scheme for power supply applications that require very high output voltage slew rates (dV/dt). The concept is to parallel 2 switching bandpass current sources, each optimized for its passband frequency space and the expected load current. The principle is demonstrated with a power supply, designed for supplying a 40W linear RF power amplifier for efficient amplification of a 16-QAM modulated data stream.

I. INTRODUCTION

Power conversion through paralleled converters is useful when an application calls for higher performance than can be achieved with a single converter. Examples are the combination of a buck converter and a linear power stage where improved load step performance is required in a DC-DC converter [1] and the combination of a class-D and a linear power stage for audio amplification with both high efficiency and low distortion [2]. In both these applications, the linear power stage supplies very little average power.

The combination of 2 or more identical switching converters is frequently seen in form of the multi-phase buck converters used for microprocessor power supplies.

In applications where both significant DC and high-frequency AC currents must be supplied, exchanging the fast, linear converter used in [1] with a high-bandwidth switching converter offers an opportunity for increasing efficiency, since a switching converter is substantially more efficient than a linear converter at high load currents.

An emerging application for DC+AC supplies is envelope tracking power supplies for RFPAs (Radio Frequency Power Amplifiers), where QAM (Quadrature Amplitude Modulation) is used.

Figure 1 Idealized example of envelope tracking supply voltage for an RFPA for 16-QAM signal amplification.

The concept of using an envelope tracking power supply for an RFPA has been well known for a number of decades [3]. The basic idea is to maximize the efficiency of a linear RFPA by supplying only the minimum necessary supply voltage at any given time, as illustrated in Figure 1. Recently, the use of switch-mode techniques [4], [5] has resulted in small and efficient envelope tracking power supplies for low-power QPSK (Quadrature Phase Shift Keying) cellular mobile telephony applications.

The increasing use of QAM over QPSK, to increase bandwidth efficiency potentially imposes higher demands on power supply output voltage slew-rate (dV/dt) due to fundamental differences between these modulation schemes. The UMTS (Universal Mobile Telecommunications System) standard for next-generation mobile telephony systems incorporates QAM in some of its higher-speed data transmission modes.

This paper examines a possible solution to designing high-efficiency envelope tracking power supplies, based on using paralleled switching power converters. The 3 main issues discussed in this paper are:

- Comparison between a single converter and the parallel configuration.
- Derivation of a suitable control method.
- Experimental verification.

II. DESIGN SPECIFICATIONS

The practical design problem considered concerns the design of an envelope tracking power supply for an X-band 40W RFPA for a satellite telephony system. The RFPA amplifies a 16-QAM modulated data stream with 150kHz symbol rate, thus requiring the power supply to effectively track a 75kHz square envelope.

The following design parameters are obtained:

Table 1 Considered design specifications.

<table>
<thead>
<tr>
<th>Specification</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Input voltage (V_{in})</td>
<td>30V</td>
</tr>
<tr>
<td>Output voltage (V_{out})</td>
<td>Between 1/3·V_{in} and 2/3·V_{in}</td>
</tr>
<tr>
<td>Output current</td>
<td>Up to 2A</td>
</tr>
<tr>
<td>Equivalent load resistance</td>
<td>10Ω</td>
</tr>
<tr>
<td>Output transition time</td>
<td>Less than 2µs</td>
</tr>
<tr>
<td>Output ripple voltage</td>
<td>As low as possible</td>
</tr>
</tbody>
</table>

Where the requirement for a 2µs transition time is set as a compromise between maximizing RFPA efficiency and minimizing the required power supply bandwidth.
III. PROPOSED POWER CONVERSION SCHEME
In high dV/dt applications, high control bandwidth is required. In the considered example, around 300kHz of closed-loop bandwidth is required, leading to a minimal switching frequency of around 1.5MHz if a single buck converter is used. In the voltage range considered, switching losses will be the dominant source of power loss, so by minimizing the current delivered by the fast switching buck converter, its efficiency can be maximized. This can be accomplished by diverting the DC load current to a slower, more efficient buck converter (see Figure 4). However, the slow converter must have a non-zero bandwidth, since the average (past to future) output current, logically enough, is unknown. It must therefore adapt to the current load current, which it should do as quickly as possible to minimize loading of the fast converter. In a proper design, the slow converter output voltage large-signal control bandwidth will be limited by inductor current slew rate, which thus provides a suitable ‘adaptation’ time constant. The impact of having non-zero control bandwidth, limited by slew-rate is shown in Figure 4.

Note that the inductor currents shown are averaged over one switch cycle (no ripple) and that output capacitor charging/discharging currents are disregarded.

IV. PROPOSED CONTROL SCHEME
The initial idea for controlling the paralleled converters is to operate the 2 output inductors as current sources, since current sources can be paralleled without problems. This requires each inductor current to be controlled individually. The Laplace-domain block diagram model of the paralleled, current-controlled buck converters is shown in Figure 3. This model is an extension of the single buck converter model utilized in [6].

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The following transfer function expressions are found:

\[
G_{c1}(s) = \frac{V_{ref}(s)}{I_{ref}(s)} = K_{mod1} G_{out}(s) \frac{G_{cfb1}(s)}{1 + G_{cfb1}(s)(K_{mod1} G_{pf1}(s) + G_{out}(s))}
\]

\[
G_{c2}(s) = \frac{V_{ref}(s)}{I_{ref}(s)} = K_{mod2} G_{out}(s) \frac{G_{cfb2}(s)}{1 + G_{cfb2}(s)(K_{mod2} G_{pf2}(s) + G_{out}(s))}
\]

\[
G_c(s) = \frac{V_{ref}(s)}{I_{ref}(s)} = K_c G_{c1}(s) + K_c G_{c2}(s)
\]

\(G_c(s)\) can be computed numerically using these expressions.

In order to minimize measurement problems, current estimation via inductor voltage integration is utilized. As discussed in [6], this method causes the current loop to transit to voltage mode when the inevitable estimator low-frequency cut-off is reached. The transition into voltage mode has the benefit of lowering output impedance [7], and is the logical reason behind calling the closed current loop around \(L_1\) a 'bandpass' current source. It is obvious that at least one of the inductor current loops has to operate down to DC, since paralleling voltage sources would be disastrous. Therefore, current estimation is only used on \(L_1\), which carries the highest RMS current. In order to ensure that the DC current in \(L_2\) is exactly 0, proportional-integral (PI) feedback is used in the current loop around \(L_2\). Thus the closed current loop around \(L_2\) is also a bandpass current source, with zero DC gain.

The derived current control scheme shown in Figure 4.

Using the selected parameters, the current controller transfer function is well behaved, and all transfer function complex pole/zero pairs have a damping factor > 1 for loads above 1Ω. Voltage control loop design is thus relatively straightforward. In order to increase mid-band loop gain (which is low at 1Ω), lag compensation is used, although this has the effect of decreasing phase margin at higher load resistances. No acceptable design is found suitable for the entire load range above 4Ω but fortunately, the converter aims for applications with load resistances above the 4-8Ω range. The problem observed is general for current controlled designs.

Table 2 Parameters used in current control loop design

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Output filter capacitor</td>
<td>(C_{out}) 200nF</td>
</tr>
<tr>
<td>Slow buck inductor</td>
<td>(L_1) 50µH</td>
</tr>
<tr>
<td>Fast buck inductor</td>
<td>(L_2) 2.2µH</td>
</tr>
<tr>
<td>Current estimator time constant</td>
<td>(\tau_{est}) 300µs</td>
</tr>
<tr>
<td>Current estimator gain</td>
<td>(K_{cfb1}) 1</td>
</tr>
<tr>
<td>Inductor sense winding ratio</td>
<td>(n_l) 1:1</td>
</tr>
<tr>
<td>PI current compensator time constant</td>
<td>(\tau_{pib}) 300µs</td>
</tr>
<tr>
<td>Slow PWM modulator gain</td>
<td>(K_{mod1}) 100</td>
</tr>
<tr>
<td>Fast PWM modulator gain</td>
<td>(K_{mod2}) 100</td>
</tr>
<tr>
<td>Slow current loop gain</td>
<td>(K_c1) 1</td>
</tr>
<tr>
<td>Fast current loop gain</td>
<td>(K_c2) 1</td>
</tr>
</tbody>
</table>

Figure 5 Combined closed-loop current controller transfer functions (\(G_c(s)\)) with different loads.

Using the selected parameters, the current controller transfer function is well behaved, and all transfer function complex pole/zero pairs have a damping factor > 1 for loads above 1Ω. Voltage control loop design is thus relatively straightforward. In order to increase mid-band loop gain (which is low at 1Ω), lag compensation is used, although this has the effect of decreasing phase margin at higher load resistances. No acceptable design is found suitable for the entire load range above 4Ω but fortunately, the converter aims for applications with load resistances above the 4-8Ω range. The problem observed is general for current controlled designs.

Table 3 Parameters used in voltage control loop design

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lag compensator pole time constant</td>
<td>(\tau_p) 10µs</td>
</tr>
<tr>
<td>Lag compensator zero time constant</td>
<td>(\tau_z) 0.82µs</td>
</tr>
<tr>
<td>Controller proportional gain</td>
<td>(K_P) 5</td>
</tr>
</tbody>
</table>
Using the voltage loop compensator parameters given in Table 3 results in the open- and closed-loop Bode plots shown in Figure 6 and Figure 7. The corresponding closed-loop step response is shown in Figure 8.

Adequate phase margin is obtained at all loads, while the output voltage settles at the correct value within 2µs for load resistances above 4Ω.

VI. SIMULATED RESULTS

A PSpice simulation model is used to verify the power supply design. As shown in Figure 9 and Figure 10, the designed control system leads to absence of DC current in L2 while maintaining stability and fast response time (≈1.5µs) over the intended load range.

V. PROTOTYPE DESIGN

Hysteresis control is used in both converters to maximize the control bandwidth per switching frequency ratio [6], [8]. The switching frequency of the fast converter is set to 1.5MHz, reflecting the control bandwidth requirement. The fast converter output filter cut-off frequency is chosen to allow sufficient output voltage slew rate. The L2 filter inductor value is chosen as a compromise between minimizing ripple current and minimizing inductor size. MOSFETs for the fast converter are chosen with emphasis on low Qg and CDS to minimize the penalty for operating at high switching frequency, leading to use of the Fairchild FDD5612. This device has the lowest Qg among considered 60V D-PAK MOSFETs (7.5nC), but still leads to switching losses being dominant.

The slow converter switching frequency is chosen for maximal efficiency. The L1 filter inductor is finally chosen so that the slow converter contributes with acceptable output ripple voltage.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Closed-loop control bandwidth</td>
<td>B_{3dB} 300kHz</td>
</tr>
<tr>
<td>Fast converter switching frequency</td>
<td>f_{sw2} 1.5MHz</td>
</tr>
<tr>
<td>Fast converter output filter cut-off frequency</td>
<td>1 \over \pi L_2 C_{oout} 240kHz</td>
</tr>
<tr>
<td>Fast buck inductor</td>
<td>L_2 2.2µH</td>
</tr>
<tr>
<td>Output filter capacitor</td>
<td>C_{out} 200nF</td>
</tr>
<tr>
<td>Slow converter switching frequency</td>
<td>f_{sw1} 250kHz</td>
</tr>
<tr>
<td>Slow buck inductor</td>
<td>L_1 50µH</td>
</tr>
</tbody>
</table>

Figure 9 Simulated output (red) and reference voltages (magenta), L2 (blue) and L1 (green) buck inductor current. Converter driving 100kHz 10Vpp+15VDC square wave response into 8Ω.

Figure 10 Simulated output (red) and reference voltages (magenta), L2 (blue) and L1 (green) buck inductor current. Converter driving 100kHz 10Vpp+15VDC square wave response into open load.
VI. ACHIEVED PRACTICAL RESULTS

A prototype power supply has been implemented, as shown in Figure 11, using a simple 2-layer PCB and low-cost components. Some precautions are necessary to prevent the 2 hysteresis controllers from synchronizing with each other through coupled switching noise. The problem is defeated through filtering at all hysteresis comparator input pins.

The 100kHz step response into 8Ω is shown along with PWM signal waveforms and inductor currents in Figure 13 and Figure 12. The response+settling time is 2µs, in accordance with specifications. The output voltage response is very similar to the simulated result (in Figure 9) apart from a small overshoot. This is probably due to unmodeled implementation dynamics in the simulation. The current responses are in good agreement, both regarding peak transient currents and ripple currents. It is especially evident that the fast converter supplies only the AC output current, while the slow converter handles the DC current, and as much AC current as allowed by L1 current slew rate. The latter leads to the slow converter ‘locking’ onto the reference signal frequency, which is a feature of the hysteresis controller.

The unloaded step response is shown in Figure 14. A small overshoot occurs as predicted by simulation.

The long-term response of the inductor currents to an output voltage step is shown in Figure 15. This measurement clearly illustrates that the slow converter operates to its maximum capability during transients, and thus that the fast converter delivers an absolute minimum fraction of the load current. The absence of DC current in L2 is also apparent.
A power loss estimation model for the paralleled buck converters has been implemented in MATLAB. Figure 16 shows a comparison between estimated and measured efficiency for a constant output voltage. A high-speed oscilloscope with current probes is used for input/output power measurement since there is significant ripple, especially on output voltage. Oscilloscope measurement errors (measurement resolution is 8 bits) should account for some of the deviation between calculated and measured efficiency.

Figure 16 Calculated and measured efficiency figures for stationary 15VDC output voltage.

VII. CONCLUSION

A power conversion and control scheme for power supplies requiring high slew-rate and high efficiency has been presented. The power conversion scheme has been compared to a simpler solution, thereby justifying the proposed, more complex, solution. Comparison with a 2-phase buck solution has proven a complicated task, at least requiring complete loss calculation models for both topologies. However, simple initial considerations show that the proposed topology probably can perform at least as well as the more common 2-phase buck topology.

A fully operational prototype has demonstrated high efficiency, considering the bandwidth and slew-rate provided. The high efficiency naturally results from the use of a highly efficient low-bandwidth converter unloading the high-bandwidth converter in parallel, allowing reduced switching losses in the high-bandwidth converter.

For the control part, operation has been explained by linear modeling, and verified both through simulation and experimental work.

The results presented in this paper are currently state-of-the-art within the field of high-bandwidth power supplies with paralleled switching power conversion.

REFERENCES